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OF

THE INSTITUTION OF ELECTRICAL ENGINEERS

FOUNDED 1871: INCORPORATED BY ROYAL CHARTER 1921

PART A

POWER ENGINEERING

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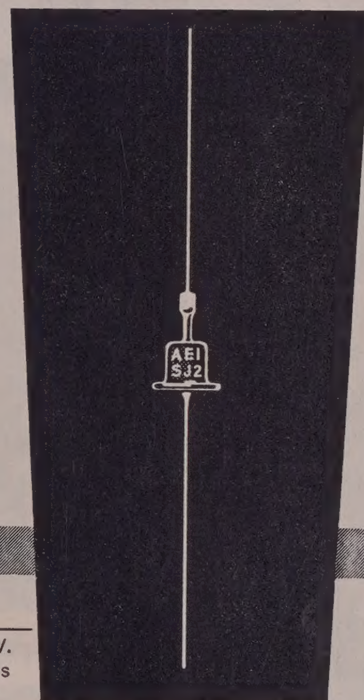

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SJ301F	300	SJ302F	300	SJ301A	300	SJ302A	300
SJ401F	400	SJ402F	400	SJ401A	400	SJ402A	400
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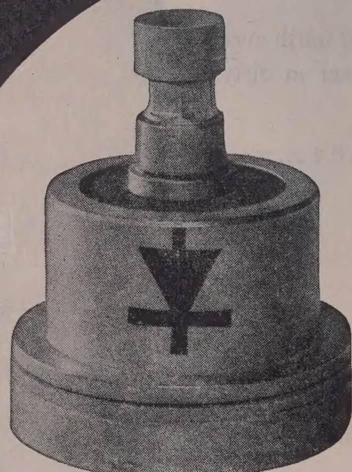

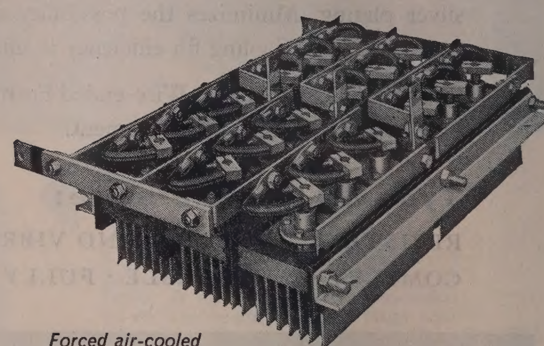

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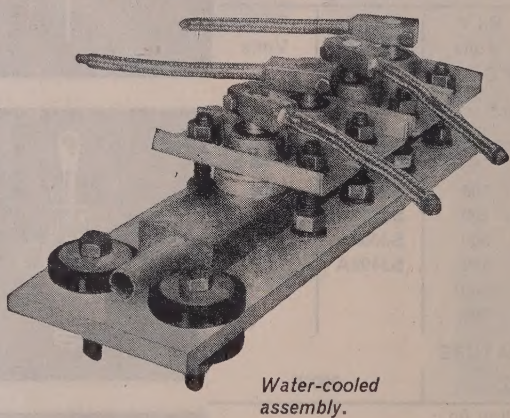

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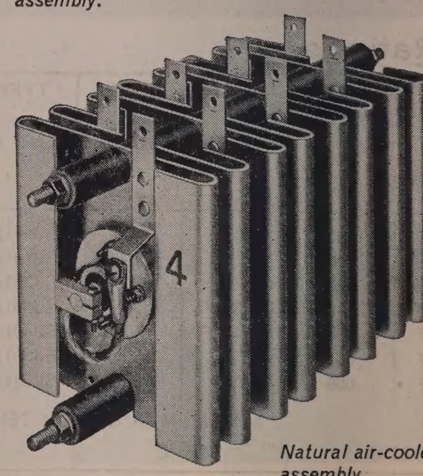

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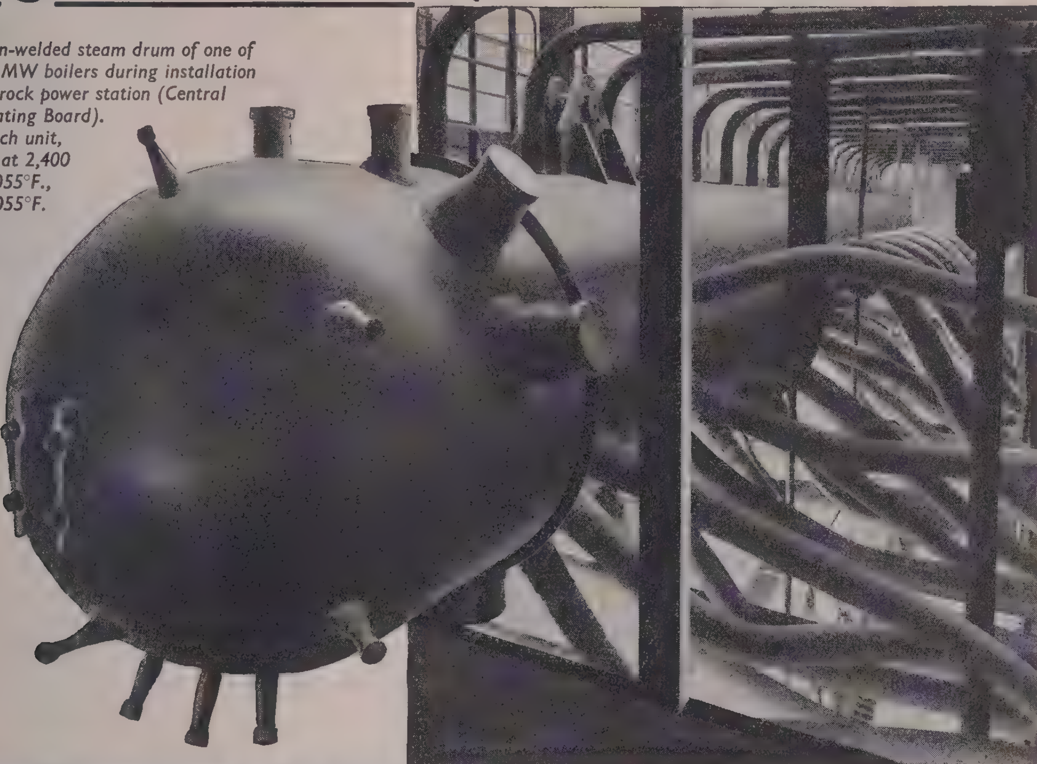

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(Right) *VENEZUELA. Babcock boilers at the Tacoa power station of La Electricidad de Caracas, including units of 550,000 lb./hr. capacity, fired by oil and natural gas. (Consulting Engineers: Sofina).*

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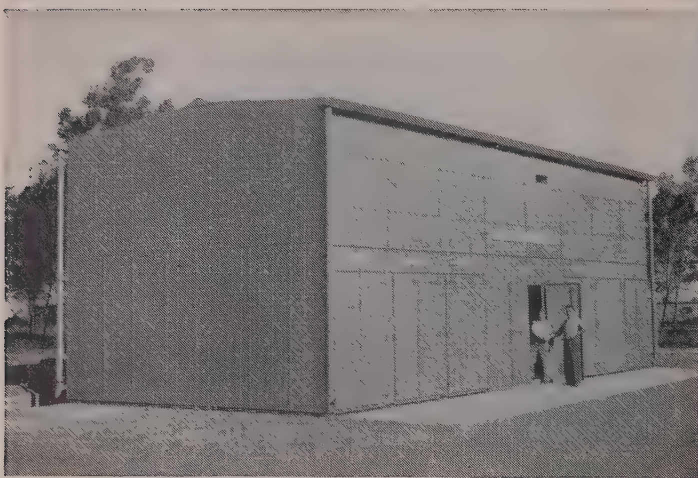

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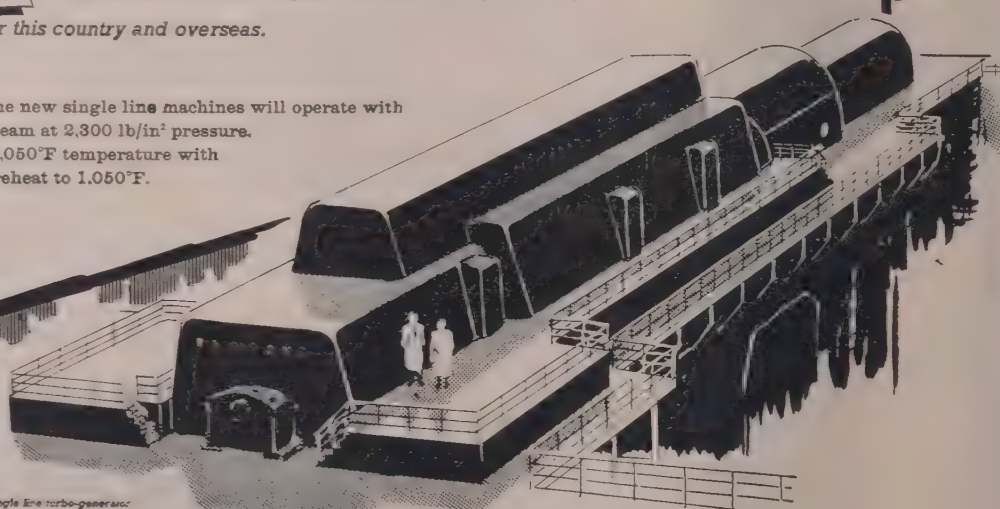

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June 1961

UNITS AND STANDARDS OF LIGHT MAINTAINED AT THE NATIONAL PHYSICAL LABORATORY, 1915–60

J. W. T. WALSH, O.B.E., M.A., D.Sc., F.I.E.S., Member, W. BARNETT, R. G. BERRY, B.Sc., Associate Member, and J. S. PRESTON, M.A., F.Inst.P., F.I.E.S., Member.

(*The paper was first received* 7*th October*, 1960, *and in revised form* 15*th February*, 1961.)

SUMMARY

Vork on photometric standards at the National Physical Laboratory n 1915 to 1960 is surveyed. The three chief aspects of the work e been, first, the establishment of the present primary standard of t, second, a study of the problems arising from the colour difference ween the primary standard and lamps in common use and the lacement of visual by physical methods of photometry, and third, improvement of the methods of deriving standards of luminous from those of luminous intensity. The main decisions of the nité International des Poids et Mesures are given, and so also is a nmary of the results of the three international comparisons con- ted by the Bureau International des Poids et Mesures in 1948, 2 and 1956.

(1) INTRODUCTION

The history of the work done on photometric standards at the tional Physical Laboratory to the time of the outbreak of the st World War is contained principally in three papers, two C. C. Paterson,[1,2] and the other by C. C. Paterson and P. Dudding.[3] The first of these papers, published in 1907, cribed the three flame standards, namely the 10-candle rnon Harcourt pentane lamp, the Hefner lamp and the rcel lamp, and summarized the results of intercomparison of se standards carried out at the standardizing laboratories of nce, Germany and Great Britain. These comparisons showed t the units maintained by means of the pentane lamp and Carcel were the same to within about 1%, while the value the Hefner unit was nine-tenths of that of the others, to hin about 2%.

This paper described also the special type of carbon-filament ctric lamp, with a single hair-pin filament mounted in a large b, developed by J. A. Fleming for use as a secondary photo- tric standard.[4]

The 1909 paper described an extensive series of comparisons between the various standardizing laboratories including, this time, the National Bureau of Standards in the United States. These comparisons were carried out by the exchange of carbon-filament electric lamps, and the relative values found for the units maintained in the various countries were:*

Great Britain (N.P.L.)	1·0000
France (L.C.E.)	1·0081
United States (N.B.S.)	1·0157
Germany (P.T.R.)	0·8961

Following these comparisons, Britain, France and the United States agreed to make the necessary small changes in the values of their respective units to bring all three to equality. The date fixed for the change was the 1st April, 1909, and it was announced in a number of scientific and technical journals in the three countries.[5] At the same time it was stated that the ratio of the Hefner unit to the new unit should be taken as 0·900. The unit agreed in 1909 was adopted by the Commission Internationale de l'Eclairage (C.I.E.) at its first post-war meeting in 1921 and was given the name 'international candle'. However, Germany was not represented at this meeting and never accepted the agreement or the name.

It should be noted here that, although the unit was, in theory, based on flame standards, the majority of photometric measurements made after 1909 were in fact made with electric lamps. Since the accuracy possible in making a photometric measurement far exceeded the reproducibility of the flame standards, it was only natural that, in practice, these standards should be almost entirely superseded by electric lamps used as secondary standards. Thus in fact the unit was maintained by means of electric lamps, although its value was based on extremely careful

ritten contributions on papers published without being read at meetings are ed for consideration with a view to publication.
his is an 'integrating' paper. Members are invited to submit papers in this gory, giving the full perspective of the developments leading to the present tice in a particular part of one of the branches of electrical science.
he paper is an official communication from the National Physical Laboratory.
r. Walsh retired in October, 1951, and Mr. Barnett died on the 22nd January, .

* National laboratories referred to in this paper are:
N.P.L.: National Physical Laboratory.
L.C.E.: Laboratoire Central d'Electricité.
C.N.A.M.: Conservatoire National des Arts et Métiers.
N.B.S.: National Bureau of Standards.
P.T.R.: Physikalisch-Technische Reichsanstalt.
P.T.B.: Physikalisch-Technische Bundesanstalt.
D.A.M.G.: Deutches Amt für Mass und Gewicht.
N.R.C.: National Research Council.
E.T.L.: Electrotechnical Laboratory.
I.M.: Institute of Metrology.

and laborious work with the flame standards. This position was recognized in the definition of the international candle by the C.I.E. in 1921. Agreement of the value of the unit in different countries was maintained by means of a periodic exchange of electric lamps measured at the different standardizing laboratories.[6]

All the flame standards were low in colour temperature, i.e. gave a much redder light than commercial electric lamps, and this raised the problem of heterochromatic photometry with all its attendant difficulties and uncertainties. It was necessary in each country to establish sets of working standards for everyday photometry and to evaluate these in terms of the secondary standards referred to above. Various methods were available for bridging the colour step, e.g. flicker photometry, ordinary direct comparison with full colour difference, the use of colour filters, etc. The method actually chosen at the N.P.L. was the so-called 'cascade method', in which the whole colour difference to be bridged was divided into five smaller colour steps by interposing four sets of secondary standard lamps operating at intermediate colours. The lamps in each set were measured by comparison with those next lower in efficiency, and the difference of colour between neighbouring sets was sufficiently small to avoid serious difficulty in making the measurements.

In this way secondary standards operating at an efficiency near to, but still rather lower than, that of the commercial vacuum lamp were prepared and the details of the work involved were fully described in the paper of 1915.[3]

It should be remarked here that, although the subdivision of a colour difference in the way described greatly reduces the uncertainty of the measurements, it does not alter any effects due to the observer's colour-vision characteristics. The extent of the variations in this respect between observers with so-called 'normal' colour vision was not then appreciated. In fact it was not until 1924 that a standard 'luminosity curve' for the average observer was adopted. There was thus no guarantee that the values assigned to the secondary standards at vacuum-lamp colour were precisely those which would have been obtained by a standard observer. The existence of this source of uncertainty was brought out very clearly by international comparisons carried out during the years 1924–26. These showed that the ratio of the Hefner candle to the international candle was about 0·87 : 1 at the colour temperature of the tungsten-filament vacuum lamp, whereas at the colour temperature of the flame standards it differed very little from the value 0·90 : 1 found in 1904.[7] This situation led the C.I.E. in 1927 to initiate a programme of work, described later, on methods of bridging the colour step between the various sets of secondary standards.

(2) EARLY DEVELOPMENTS

After the First World War, the rapid development of the gas-filled lamp raised two major problems in photometric measurement. In the first place, the colour of the new lamps was considerably 'whiter' than that of the vacuum lamps. In fact the colour difference between the two was similar to that between the vacuum lamp and the old flame standards. In the second place, the emphasis had shifted from luminous intensity or candle-power, in a single direction or group of directions, to luminous flux, a measure of the total light output from a lamp irrespective of direction.

(2.1) Secondary Standards of Luminous Flux

The unit of luminous flux, the lumen, was defined as the flux emitted within unit solid angle by a uniform point source having a luminous intensity of one candle, so that the total flux output of a lamp was equal to its mean spherical candle-power multiplied by 4π. The two units were thus formally related and it was necessary to establish secondary standards of luminous flux evaluated in lumens, derived from the secondary standards of luminous intensity evaluated in candles. This was done in 1921–22 by measuring the mean spherical candle-power of lamps giving as nearly as possible an axially symmetrical light distribution. The lamps were of the vacuum type with a 'squirrel cage' form of filament. They were rotated in either the pendent or the horizontal position, according to size, and the mean intensity distribution curve in a plane containing the axis was determined by a point-by-point method. From this curve the mean spherical candle-power, and hence the flux in lumens, was determined by Rousseau's construction.

Once a set of secondary standards of luminous flux had been prepared in this way, other lamps could be evaluated from them by a single measurement in an integrating sphere. This process was used for preparing working standards of luminous flux and a set of secondary standards of flux at about the colour of the commercial gas-filled lamp. The method used for bridging the colour step was not, in this case, the cascade method but a direct measurement with an equality-of-brightness photometer. A substitution method was used and, by placing a suitable colour filter on the comparison-lamp side of the photometer, the colour difference involved in each part of the measurement was approximately half the total difference between the two sets of secondary standards.

(2.2) The Basis of Heterochromatic Photometry

When the C.I.E., at its meeting in 1924, adopted a table of values of the luminosity function (now termed the 'relative luminous efficiency of radiation'), it standardized, by implication, the basis for any heterochromatic photometric measurement, and in 1927, after a meeting of the C.I.E., representatives of the national standardizing laboratories initiated a programme of work on a set of blue glasses which, when used with a source of the colour of the carbon-filament secondary standards, gave light which matched in colour and, to a fair approximation, in spectral distribution the light from a tungsten-filament vacuum lamp. Colours were conveniently expressed by the appropriate values of 'colour temperature', i.e. the temperature of the full (black-body) radiator which had the same colour and, to a close approximation, the same spectral distribution. The colour temperature of the carbon-filament secondary standards was about 2080° K and that of tungsten-filament vacuum lamps was about 2360° K, and the colour step bridged by the international blue glasses was from 2045 to 2350° K. Since the same glasses would bridge the colour step from 2350 to about 2760° K they could, fortunately, be used, not only for comparing tungsten-filament vacuum-lamp secondary standards with lamps of about the same colour as the carbon-filament secondary standards, but also for comparing gas-filled secondary standards, operating at a colour temperature of about 2760° K, with the vacuum secondary standards.

The spectral transmission curves of all four glasses were determined at the four national standardizing laboratories of France, Germany, the United States and Britain. One glass was retained at each laboratory and the value of the transmission factor assigned to it was the mean of the four values obtained from the spectral transmission curves determined at the four laboratories, using the formula

$$\frac{\int E_\lambda t_\lambda V_\lambda d\lambda}{\int E_\lambda V_\lambda d\lambda}$$

where E_λ is the energy radiated per unit wavelength interval at wavelength λ by the source with which the glass is used, V_λ is

e international relative luminous efficiency of radiation and t_λ the transmission factor of the glass, both at wavelength λ. ne work on these glasses was completed in 1930,[8] and the sults were reported to the C.I.E. at its meeting in 1931.

(3) STAGES LEADING TO THE INTRODUCTION OF NEW UNITS IN 1948

From what has been stated it will be seen that from about 09 there had been, in effect, no primary standard of light. The d flame standards had been practically, though not officially, scarded and the photometric units, the candle and the lumen, ere maintained by means of electric lamps. These were inter-mpared at intervals to ensure that the magnitude of the units s constant, but the situation was clearly unsound funda-entally and the need for a satisfactory primary standard was ly realized. The most attractive form of standard, at any e from a theoretical point of view, was that originally proposed 1908 by Waidner and Burgess,[9] namely a full radiator at the nperature of solidification of molten platinum, the unit of ninous intensity being defined in terms of the luminance rightness) of this standard. For years this proposal remained thing more than an interesting possibility, but from about 29 serious attempts were made to give it practical form and, 1930, work done at the National Bureau of Standards was ported to the Comité Consultatif d'Electricité of the Comité ernational des Poids et Mesures (C.I.P.M.).[10, 11]

This advisory committee had been authorized by the C.I.P.M. 1930 to extend its functions to include work on international otometric units and standards, and in 1937 a separate advisory mmittee, the Comité Consultatif de Photométrie, was formed take charge of the photometric work and to be responsible r it to the C.I.P.M. It was therefore no longer left to the ndardizing laboratories to act on their own initiative in ranging international comparisons or undertaking other work the photometric standards; this responsibility was trans-red to the body which had long been the custodian of the ernational standards of mass and length and which had cently assumed responsibility for the electrical units as well.

(3.1) First Measurements of the New Primary Standard

The form of the primary standard of luminous intensity used r the work reported to the C.I.P.M. in 1930 is shown in Fig. 1. e radiator was the central tube, and an image of the opening the top was formed on a photometer head by means of a lens d prism system, so that the luminous intensity of a secondary-ndard lamp could be determined in terms of the luminance the radiator. The platinum, of a high degree of purity, was ated to just above its melting-point by means of a high-quency induction furnace and was then allowed to cool wly. The standard luminance was that observed during the rest' period, i.e. the period when the luminance remained nstant during the course of the solidification of the platinum. more complete description of the apparatus is given in the pers cited above and in other later publications. These papers o contain accounts of the methods used for determining the nsmission factor of the prism-lens system.

Apparatus of a similar design was set up at the N.P.L. and at ner laboratories, and the luminance of the standard was termined in terms of the international candle. The results ported in 1937 to the Comité Consultatif de Photométrie of e C.I.P.M.[12] were as follows:

	candles/cm²
United States (N.B.S.)	58·86
Great Britain (N.P.L.)	58·96
France (Univ. of Strasbourg)	58·78

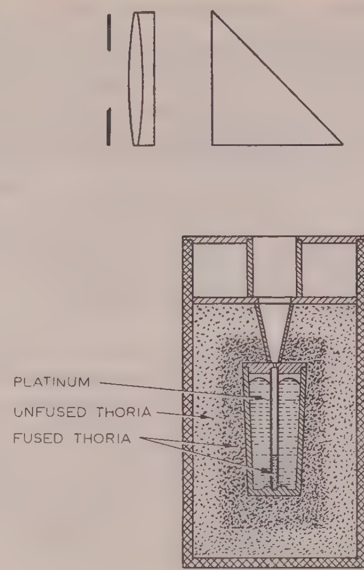

Fig. 1.—Original form of the primary standard of light used in 1930 at the National Bureau of Standards, U.S.A.

A very similar form has been used for subsequent work at the N.P.L., but latterly, for health reasons, zirconia powder has replaced the unfused thoria.

giving a mean value of 58·9 with an estimated uncertainty of $\pm 0 \cdot 2$ candle per square centimetre.

At the same meeting of the Comité Consultatif the proposal was made[13] to re-establish the photometric scale on the basis of (*a*) a new candle, of such a magnitude that the luminance of a full radiator at the temperature of solidification of platinum was precisely 60 new candles per square centimetre and (*b*) the use of the international luminosity function for the evaluation, in terms of this unit, of any light differing in colour from that of the primary standard.

This proposal had two great practical advantages. In the first place Germany agreed that, if it were adopted, there would be no need to retain the Hefner unit, and thus the inconvenience of the co-existence of two units differing in magnitude by about 10% would be removed. In the second place the only practically important change entailed was a comparatively small one in the values assigned to lamps of the tungsten-filament vacuum type. This arose from the fact that the secondary standards used to measure such lamps had been derived from those at the colour temperature of the old flame standards, by a method which was now found to give results not exactly in accord with the international relative luminous-efficiency function, V_λ. In fact the difference was found to be about 1·3%, and it was of such a sign that it reduced the difference of 1·9% produced by the change in the magnitude of the unit. Thus, although the values assigned to lamps operating at the colour temperature of the primary standard (approximately 2042° K) were increased by 1·9%, for lamps operating at the practically much more important colour temperature of 2360° K the increase was only about 0·6%. At still higher colour temperatures the difference became progressively less, and for gas-filled lamps operating at normal efficiency it became a decrease in value of the order of 1% or slightly more.

The proposal was adopted by the C.I.P.M. in the following form[14]:

1°. A partir du 1er janvier 1940, l'unité d'intensité lumineuse sera telle que la brillance du radiateur intégral, à la température de

solidification du platine, soit de 60 unités d'intensité par centimètre carré.

Cette unité sera appelée la 'bougie nouvelle' (avec traduction appropriée dans les autres langues).

2° a. Les valeurs des grandeurs photométriques des sources lumineuses ayant une couleur autre que celle de l'étalon primaire seront déterminées par un procédé tenant compte de la courbe des facteurs de visibilité (luminosité) adoptée par le Comité international des Poids et Mesures.

b. Pour assurer aux instituts metrologiques des différents pays l'uniformité dans le procédé de passage du nouvel étalon primaire aux étalons secondaires à filament incandescent présentant un rendement photométrique plus élevé, on adopte présentement la méthode des filtres qui, intercalés entre le photomètre et l'une des sources lumineuses à comparer, rétablissent la sensation de couleur identique sur les deux plages de l'écran photométrique.

A further resolution passed at the same meeting of the C.I.P.M. called on the national laboratories each to prepare two groups of six lamps, one group operating at the colour temperature of the primary standard and the other at a colour temperature of 2 360° K. These lamps were to be measured in terms of the new candle, the method used for evaluating the second group being in accordance with resolution 2 quoted above, and they were then to be sent to the N.P.L., which was charged with the duty of comparing them and furnishing the C.I.P.M. at its next meeting with a report on the results.

(3.2) Vacuum-Lamp Secondary Standards

In order to comply with the resolutions of the C.I.P.M. it was necessary for the N.P.L. to prepare two sets of lamps for use as secondary standards of luminous intensity at (*a*) the colour temperature of the primary standard, namely 2 046° K on the temperature scale in use at that time or 2 042° K on the present scale, and (*b*) the colour temperature resulting from the use of the international blue glass with a lamp which colour-matched the primary standard, i.e. about 2 350° K.

The lamps used had uniplanar filaments in grid form and the details of the design, shown generally in Fig. 2, were worked

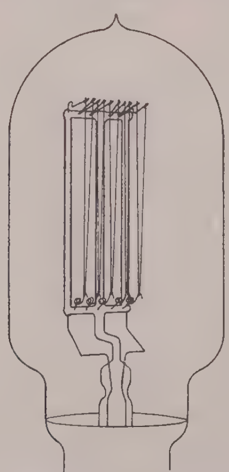

Fig. 2.—Vacuum lamp with uniplanar tungsten filament of the type used for fundamental standards of luminous intensity at 2 042 and 2 350° K.

out in collaboration with the manufacturers. It will be noticed that the lower supports for the filaments were provided with light springs to accommodate the lengthening of the filament when incandescent. They had sufficient tension to ensure that the local cooling, due to the contact with the support, was constant during operation.

Each lamp was mounted permanently in a long stem, fitting snugly into a photometer-bench carriage, and leads were permanently soldered to the lamp contact plates and brought to two terminals, one on each side of the upper part of the stem (see Fig. 3). When the lamp was in use two leads were con-

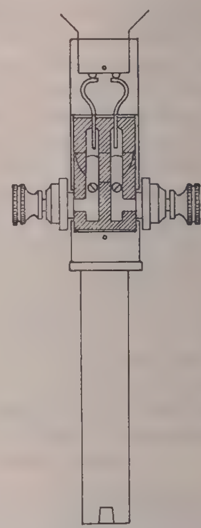

Fig. 3.—Lamp stem for mounting the type of lamp shown in Fig. 2.

nected to each terminal; one carried the current and the other was used for measuring the voltage applied to the lamp at the ends of the permanently soldered leads. The lamp was set by this voltage, but a measurement of current was made during each period of use. Each lamp was mounted so that when in use the plane of the grid filament was perpendicular to the bench axis. This was ensured by means of the tapered slot shown at the bottom of the stem. This slot engaged in a key-piece of similar shape permanently fixed inside the hollow shaft forming part of the bench carriage. The distance between the lamp and the photometer surface was always measured to the plane of the filament. No allowance was made for the fact that the image of the filament in the cylindrical part of the bulb was not, in every case, accurately in the plane of the filament, but the lamps were always used at such a distance from the photometer surface that the illumination of that surface was 10 lx. The approximate luminous intensity of the lamps in the first set was 13 new candles, while in the case of the higher efficiency set it was 24.

When a blue glass was used two corrections were needed. The transmission factor, approximately 0·52, was that of the glass as a whole, including the loss by reflection at the glass–air surfaces but with no allowance for the shortening of the effective light path. Consequently, when a glass was placed between a lamp and the photometer head, it was necessary to subtract from the measured distance on the bench a distance equal to one-third of the thickness of the glass, which was approximately 0·95 mm thick. A more considerable correction was that made necessary by inter-reflections between the photometer surface and the glass, which was, for convenience, mounted on the window of the photometer head, about 46·5 mm away from the surface. This correction amounted to about 0·33%.

The luminous intensities of the 14 secondary standards matching the primary standard were measured by 14 observers who compared them visually, using a Lummer–Brodhun contrast photometer head, with 17 lamps the values of which were known

terms of the primary standard, some of these having been d in 1931–34 for determining the luminance of the primary ndard in terms of the international candle. They were taken m two sets of existing N.P.L. secondary standards which med part of the 'cascade' series referred to earlier, one set, Fleming–Ediswan single-loop carbon-filament lamps, having olour temperature of about 1975° K, while the other set, gsten-grid filament lamps, had a colour temperature some higher.* Since the luminous intensities of these 17 lamps e thus known in terms of the luminance of the primary ndard, the luminous intensities of the new set of secondary ndards, henceforward referred to as Group A, could be ived in terms of the new candle.

he second set of 20 new secondary standards, henceforward rred to as Group B, were then measured by comparison n the 14 lamps of Group A used in conjunction with the rnational blue glass. This measurement was again made ally by 14 observers using the same type of photometer as ore. It is worth remarking that in this comparison there is a y slight colour difference owing to the fact that the blue ss, when used with a lamp operating at the lower colour tem- ature, does not give an exact spectral match with a lamp rating at the higher colour temperature. Although the dual colour difference is very slight, the difference of energy ribution in the spectrum is by no means inappreciable.[15]

(3.3) International Intercomparison of the New Candle

or the purpose of the intercomparison called for by the P.M. in 1937, seven lamps of Group A and ten of Group B e used and these were intercompared with the sets of lamps t by the other national laboratories. In accordance with the P.M. decision mentioned above, all the sets of lamps were rcompared at the N.P.L. The results showed that the values gned to the new candle at the five laboratories which partici- ed in this comparison differed only by small amounts, as will seen from Table 1.[16]

Table 1

COMPARISON OF VALUES ASSIGNED TO THE NEW CANDLE

Laboratory	Relative values of new candle	
	at 2046° K	at 2360° K
United States (N.B.S.) ..	0·9990	0·9994
Germany (P.T.R.)	0·9964	0·9931
France (L.C.E.)	1·0061	0·9972
Japan (E.T.L.)	0·9968	1·0084
Great Britain (N.P.L.)	1·0016	1·0018

he work was then interrupted by the outbreak of war, but ertheless the values assigned to the lamps of Groups A and B he N.P.L. were altered so as to bring the value of the unit his laboratory into agreement with the mean value of the as established by the five laboratories, the values of the ps in Group A being increased by 0·16%, those of Group B,)·18%.

(3.4) Derivation of Secondary Standards of Luminous Flux

Vhile the work described above was in progress, steps were g taken to derive secondary standards of luminous flux from se of luminous intensity. Although in practice such secondary ndards were required mainly at the colour temperature of a filled lamp, the most convenient method of deriving them olved the rotation of a lamp about its axis, so that it was necessary to use vacuum lamps for the purpose. The first step was to prepare special lamps having a light distribution as symmetrical as possible about the lamp axis. The design adopted is shown in Fig. 4. The coiled filament consisted of two portions in parallel, each forming one half of an octagon, so that there was no discontinuity. A small disc collar was secured to the cap for a reason that will become clear later.

* These were sets 2 and 3 described by Paterson and Dudding,[3] p. 271.

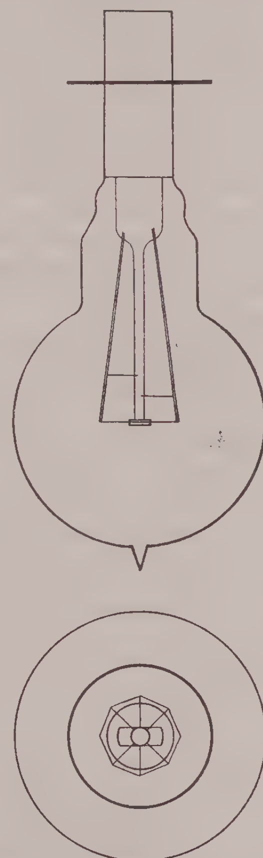

Fig. 4.—Special design of lamp for use in the rotator shown in Fig. 5.
It has a light distribution which is very symmetrical about the axis of rotation.

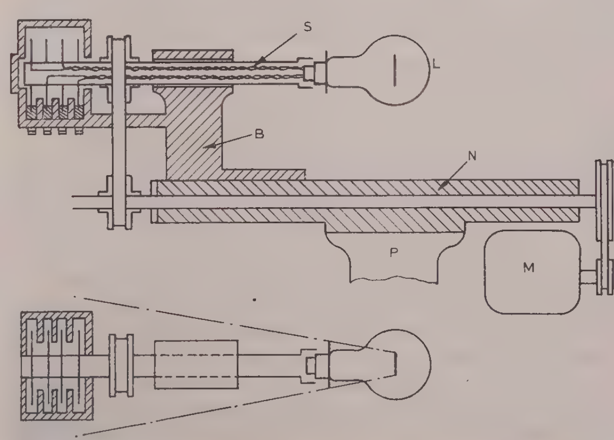

Fig. 5.—Section of the lamp rotator used in preparing fundamental standards of luminous flux.

The lamps were rotated with their axes horizontal in the rotator, shown diagrammatically in Fig. 5, which was designed and constructed in the Metrology Division of N.P.L. N is a solid metal arm carried on a heavy metal pedestal, P, and capable of being moved by hand about a vertical axis at the centre of P. It carries a motor, M, by means of which the hollow shaft, S, is rotated about its bearings within the support, B. At its free end, S is fitted with a special lamp socket in which the lamp, L, can be firmly secured in position, and B can be moved along guides in N so that the light centre of L can be brought accurately into the vertical axis of rotation of the arm, N. A pair of flexible leads is taken from each lamp contact, and these leads are connected to the four copper discs dipping into mercury cups. One lead of each pair carries the current supply to the lamp, while the other is used for measuring the voltage at the lamp contacts.

The rotator was placed opposite one end of a photometer bench and the two were accurately aligned so that when a lamp was in position its light centre lay on a line parallel to the bench axis and passing through the centre of the Lummer–Brodhun photometer head mounted on the bench. A lamp was fixed in the rotator and was spun about its horizontal axis at such a speed that there was no appreciable flicker in the photometer; quite a low speed was found sufficient in the case of the lamps described above. The mean luminous intensity could then be measured at any particular angle from the lamp axis simply by moving N into the appropriate position.

The special lamps described above and shown in Fig. 4 were measured in this rotator by the substitution method, using the lamps of Group B as secondary standards and a combination of visual and photo-electric methods for evaluating the luminous flux. The first step was to determine as accurately as possible the mean distribution curve for each lamp. This was done with the sensitive photo-electric photometer, consisting of a thin-film K–O–Ag photo-emissive cell and amplifier, described elsewhere.[17] Measurements were made in a sufficient number of directions to enable the form of the curve to be determined with certainty. From this curve a selection was made of several, usually five, directions in which the luminous intensity was almost independent of small angular displacements, and in these directions a series of visual measurements were made by six observers so that the scale of the distribution curve was established by reference to the lamps of Group B. It will be noticed that no colour difference was involved in this measurement so that the number of observers needed was smaller than in, for instance, the measurement of the Group B lamps in terms of Group A. The luminous flux given by the lamp was found from the distribution curve by means of a calculation equivalent to the Rousseau construction.

The small discs on the lamp caps, mentioned earlier and shown in Fig. 4, were of such a diameter that, as the angle from the forward prolongation of the lamp axis approached 180°, the light reaching the photometer was cut off by this disc and not by any portion of the rotator. Thus the flux determination was made for the lamp and disc combined, and this combination formed one of the secondary standards of luminous flux, henceforward referred to as Group F.

The lamps of Group F gave about 280 lm and operated, of course, at the same colour temperature as Group B, i.e. about 2 350° K. The next step was to prepare a set of secondary standards of considerably higher luminous flux. These were needed as intermediaries between the lamps of Group F and the secondary standards of flux designed to operate at a colour temperature of about 2 800° K, because at this higher colour temperature it was necessary to use 500 W gas-filled lamps giving about 6 500 lm. Also, in evaluating them it was necessary to use the blue glass with the lower-efficiency lamps, so that, with out any intermediate lamps, the ratio of the luminous fluxe from the lamps to be compared would have been about 45 : 1.

The lamps used for the intermediate set, called Group G, wer gas-filled lamps with festoon filaments welded to their support to give greater steadiness in use. In this and in other respect they were of special design and construction, but in genera appearance they resembled 1 000 W general-lighting-servic lamps. When underrun to colour-match the lamps of Group I they gave about 2 400 lm. They were measured by comparisor with the eight lamps of Group F, using a 10 ft spherical integrato and a photo-electric photometer.

(3.5) International Intercomparison of the New Lumen

In addition to the intercomparison of lamps measured fo luminous intensity, the C.I.P.M. resolution of 1937 included a recommendation for a preliminary international comparison o the lumen at the colour temperature of approximately 2 350° K This intercomparison, also, was carried out at the N.P.L. an gave the results shown in Table 2.[18]

Table 2

Comparison of Values Assigned to the New Lumen

Laboratory	Relative value of new lumen at 2 350° K
United States (N.B.S.)	1·001
Germany (P.T.R.)	1·010
Japan (E.T.L.)	0·994
Great Britain (N.P.L.)	0·994

The luminous intensity of each lamp in a specified directio was measured, as well as the luminous flux, so that it was possibl to allow for differences in the value of the new candle used a the different laboratories. This allowance was made in derivin the values given above, so that these represent only the difference introduced during the course of determining the lumen from th candle. The number of lamps used, namely three, was, howeve insufficient to provide more than a rough indication of th position as it was in 1938.

(3.6) Secondary Standards of Luminous Flux at 2800° K

The measurement of the secondary standards of Group C was the last step in the work to be completed before the outbrea of the Second World War. The final step, the preparation o secondary standards of luminous flux at the colour temperatur obtained by using the international blue glass in combinatio with a lamp of Group G, had to be deferred although suitabl lamps for the purpose were obtained. These were 12 lamps similar in appearance to 500 W general-lighting-service lamps but again specially designed and constructed to secure th maximum degree of steadiness in operation. When slightl underrun to the required colour temperature, about 2 800° K they gave approximately 6 500 lm. These lamps were designate Group H, and their measurement by comparison with Group C was carried out visually by 12 observers, again using a 10 f sphere.

(3.7) Introduction of the New Units

It had been planned that the new photometric units, the 'nev candle' and its derivatives, should be introduced in 1940,[19] bu this was prevented by the outbreak of war and the matter remaine in abeyance until 1946, when it was decided by the C.I.P.M.[2 that this action should be taken on the 1st January, 1948.*

* This decision was subsequently endorsed by the Conférence Générale des Poid et Mesures at its ninth session in 1948.

om that date, all photometric measurements made at the P.L. have been expressed in terms of the new units. The ne 'new candle' was shortly afterwards changed to 'candela' breviation, cd), but no distinction was made in the names of other units.

4) NATIONAL AND INTERNATIONAL WORK SINCE 1948

1) The First Two International Intercomparisons in Terms of the New Units (1948 and 1952)

Three international intercomparisons have been carried out ce the introduction of the new units. The first took place in period 1948–49 and may be referred to as the intercomparison 1948. It has been described fully in a report made to the mité Consultatif de Photométrie at its meeting in 1952.[21] e comparison covered both the candela and the lumen, the mer at the two colour temperatures 2042 and 2353° K and latter at 2353 and 2788° K. It was carried out at the Bureau ernational des Poids et Mesures, each national laboratory ding a group of measured lamps to Sèvres for intercomparison the B.I.P.M. photometric laboratory. The N.P.L. sent six nps measured in candelas, three at each colour temperature, l twelve lamps measured in lumens, six at each temperature.

Table 3

ELATIVE VALUES OF PHOTOMETRIC UNITS: 1948 COMPARISON

Country	Candela		Lumen	
	2042° K	2353° K	2353° K	2788° K
ermany (P.T.B.–D.A.M.G.)	1·0023	1·0045	1·0042	1·000
nited States (N.B.S.) ..	0·9988	1·0007	1·0011	0·995
rance (C.N.A.M.–L.C.E.)	1·0045	1·0018	1·0100	—
reat Britain (N.P.L.) ..	0·9929	0·9977	0·9905	1·009
apan (E.T.L.)	1·0109	0·9944	0·9992	—
.S.S.R. (I.M.)	0·9905	1·0010	0·9949	0·997

The results of the intercomparison are given in Table 3, ich shows the values of the candela and the lumen at each ional laboratory in terms of the mean, which is taken as unity.

The second international intercomparison was planned immetely after the completion of the first, as it was thought that agreement might be improved through the experience gained the various laboratories during the course of the first comison and subsequently. This comparison was carried out the B.I.P.M. during the period 1950–52 and the results were orted to the Comité Consultatif de Photométrie in 1952.[22] ain the N.P.L. sent six lamps for the comparison of the candela l twelve lamps for the comparison of the lumen.

n most cases the figures obtained differed very little from se found in the intercomparison of 1948 as will be seen from ble 4.

Table 4

ELATIVE VALUES OF PHOTOMETRIC UNITS: 1952 COMPARISON

Country	Candela		Lumen	
	2042° K	2353° K	2353° K	2788° K
nited States (N.B.S.) ..	0·9973	0·9988	0·9994	0·997
rance (C.N.A.M.)	1·0062	1·0032	1·0086	0·998
reat Britain (N.P.L.) ..	0·9980	0·9983	1·0041	1·008
apan (E.T.L.)	1·0038	0·9978	0·9955	1·001
.S.S.R. (I.M.)	0·9948	1·0020	0·9924	0·996

The German laboratories did not participate in this comparison.

In this intercomparison, the lamps sent by each national laboratory were returned to that laboratory for remeasurement after the completion of the measurements at the B.I.P.M. The mean of the two values obtained at the national laboratory was taken in arriving at the figures tabulated.

(4.2) New Work on the Primary Standard and the N.P.L. Photometric Scale

In 1950–51, just before this intercomparison, a new determination of the candela had been made at the Conservatoire des Arts et Métiers, using the primary standard. In terms of the older international candle, the luminance of the primary standard was found to be 58·82 candles per square centimetre as compared with the value 58·78 found at the University of Strasbourg in 1937.[12] In Table 4, the figures for France are based on this newer determination. None of the other countries, however, had redetermined the unit between 1948 and 1952.

The differences between the units of the various national laboratories revealed by the 1948 and 1952 intercomparisons made it desirable for each country to check its photometric scale.

In the period 1954–56 the primary standard was set up for the second time at the N.P.L. using a modern high-frequency furnace.[23, 24] About 140 determinations were made covering seven platinum ingots. Both visual and photo-electric methods were used, but there was no significant difference between the respective mean values obtained. The photo-electric photometer was a selenium photo-voltaic cell approximately corrected to standard observer response by a glass filter, and used only as a comparator, not as a measuring device. Using Group A as reference standards, the mean luminance of the primary standard was found to be 59·7 cd/cm^2 on the same N.P.L. scale as was in force at the time of 1952 intercomparisons.

No adjustment has been made to the N.P.L. photometric scale as a sequel to these measurements, because (*a*) the result would have displaced the N.P.L. candela at 2042° K still further from the international mean unit derived from the 1952 intercomparisons, and (*b*) the renewed experience gained in the use of the primary standard suggested that an appreciable part of the observed change of 0·5% could be due to variability in the standard itself and so did not with certainty represent a real improvement in absolute accuracy.

The values assigned to the lamps of Group A have therefore remained unchanged, but check measurements were made on the rest of the photometric scale. For the work, photo-electric methods were used exclusively at the N.P.L. The detector was an Osram K.M.V. 6 photo-emissive cell closely corrected to the standard observer spectral response by means of a Preston-type liquid filter.[25] The photocell, filter and associated circuit, which was of the electrometer valve-voltmeter type, was kept at a constant temperature of 25° C.

The international blue glass was not used with this artificial eye. The small errors caused by the residual mismatch between the standard observer data and the overall spectral sensitivity of the photometer were corrected by calculation. For this purpose the lamps were assumed to have a black-body energy distribution of the appropriate temperature.

In addition to the replacement of the visual by the photoelectric method, another change was made in the procedure. Instead of using all the lamps in Groups A and B at the individual distances giving an illumination of 10 lx at the photometer, all of them were used at the *mean* distance appropriate to Group A. At this distance, lamps of Group B gave an illumination of about 17 lx. Moreover, the shortening of the distance, combined with an expected departure from the inverse square law, resulted in a change of about 0·25% in the book-values of intensity

obtained for Group B. Some changes, not exceeding 0·3%, were found in other Groups, but in these cases they were not due to any known systematic changes in procedure.

Two additional groups of lamps have been added to the N.P.L. photometric scale. The first of these, Group E, is a set of eight squirrel-cage lamps of special construction evaluated for total luminous flux on the lamp rotator. The colour temperature of Group E is the same as that of Group F, namely 2350° K. In fact these two Groups are now of equal status. It was felt most desirable, for calibrating working standard lamps, to have available two groups of fundamental standard lamps of differing luminous-flux distributions, because of the possible errors attendant on the direct comparison of different types of lamp in an integrating sphere.[26] Groups G and H, which follow groups E and F in the scale, have in fact an intermediate distribution.

The second additional group of lamps, designated C, are straight-wire uniplanar-filament gas-filled lamps operating at a temperature of 2 854° K and calibrated for luminous intensity.[27,28] These have conical bulbs to deflect back-reflections from the line of observation, and an opaque black coating, with an aperture just larger than the filament, is sprayed on the front of the bulb to obscure light from the filament mounting and from images in the bulb. These arrangements, together with the small size of the filament, 17 × 18 mm, ensure a close approximation to inverse-square-law behaviour. However, the variation of intensity for small angles of tilt is rather large, up to 0·25% per degree.

During the evaluation of Group C lamps, Group B were used at their new assigned distance, while two sets of measurements were made on Group C, first at the same distance as Group B, second at the distance required to give the same mean illumination as Group B. This was done because of the much higher intensity (260 cd) of Group C and to test the success of a technique devised to overcome some photocell fatigue experienced when Group C were used at the shorter distance giving an unusually high illumination on the photometer. A mean difference of only 0·1% was observed between the results of these two procedures.

(4.3) The Third International Intercomparison (1956) and the Present Position

The third international intercomparison took place at the B.I.P.M. in the period 1956–57, and the results were reported to the fourth meeting of the Comité Consultatif de Photométrie in 1957. The number of participating laboratories was brought up to seven by the inclusion for the first time of the National Research Council of Canada.

Twelve lamps were sent by the N.P.L. for the candela comparison, six at colour temperature 2042° K and six at 2353° K. These lamps had the etched-frame type of filament mounting,[29] which gave a much improved light distribution in a horizontal plane.

As in previous comparisons, six vacuum squirrel-cage lamps were sent for the lumen comparisons at 2353° K. In addition, four vacuum lamps with complete ring-type filaments of the same type as in Group F (Fig. 4) were sent. These two groups were calibrated against groups E and F respectively. The two values of the lumen at 2353° K, thus derived and also submitted to the B.I.P.M. in terms of the lamps of the two different filament types, were found by the B.I.P.M. to differ by only 0·2%.

The six lamps sent for the lumen comparison at 2788° K were of the same design as those used for Group F (Fig. 4), but were larger (200 W rating) and gas-filled. This design was chosen because a 100 W version[29] has been in use at N.P.L. for a number of years and had proved capable of high precision.

All the N.P.L. lamps were fitted with Edison screw-caps to conform to B.I.P.M. requirements; therefore a constant value o lamp current rather than lamp voltage was set for each lamp in view of possible variation in contact resistance at the lam socket. At the same time it was decided, for all lamps, to specif exactly the time interval which should elapse between switching on and making the photometric measurement. Changes occu while a lamp is reaching thermal equilibrium, and these ar quite significant during the first 10 min or so. Moreover, the are usually of opposite sign for lamps operated at constan voltage and at constant current respectively.[30]

With the completion of the 1956 intercomparison[31] it wa found possible, for the five laboratories which had participate in both 1952 and 1956, to express all the results in terms of th 1952 scales of those laboratories. It was thus possible to tak an overall average, for each of the four units, of five laboratorie on two separate occasions. It was proposed that these fou overall averages should be accepted as an international unifie scale, and that all the seven laboratories participating in 195 should make the small adjustments necessary to bring thei units into exact alignment with it. The proposal was accepte by France, Germany and Japan. The remainder, however, con sidered that the necessary adjustments did not exceed th statistical fluctuations as from one country to another, and fron one comparison to another, and that to make them would no therefore ensure closer alignment for the future. They did no therefore accept the proposal, although each expressed it willingness to do so had the others been unaminously in favou of acceptance. Table 5 shows the relative values of units i force in March, 1958, on this basis.

Table 5

RELATIVE VALUES OF PHOTOMETRIC UNITS IN FORCE SINCE 1958

Laboratory	Candela		Lumen	
	2042° K	2353° K	2353° K	2788° K
	%	%	%	%
Germany (P.T.B.-D.A.M.G.)	0	0	0	0
United States (N.B.S.) ..	+0·1	−0·1	0·0	0·0
Canada (N.R.C.)	−0·5	−0·3	0·0	+0·9
France (C.N.A.M.) ..	0	0	0	0
Japan (E.T.L.)	0	0	0	0
Great Britain (N.P.L.) ..	−0·1	−0·1	−0·2	−0·7
U.S.S.R. (I.M.)	−0·5	−0·2	−0·3	−0·4

It was agreed at the 1957 meeting of the Consultative Com mittee that improvement in the precision obtainable with preser primary standard techniques would be very desirable. Furthe research was decided on by several national laboratories wit this object. Britain was already investigating the possibility c placing the unit of light on a radiometric basis,[23] and it wa agreed that this work should be continued as the specific contr bution of the N.P.L. to this general subject.

It was agreed to continue intercomparisons at four-yearl intervals in the future, and for the next intercomparison it wa planned to exchange an additional group of intensity standarc operating at the (colorimetrically) important colour temperatur of 2854° K.

(5) CONCLUSION

The chief question raised by the results of the intercomparisor conducted hitherto concerns the accuracy with which the uni of one country can be compared with those of another, by th present procedures, if all things are taken into consideratior

e limits have been thought to be about $\pm 0{\cdot}2$ or $0{\cdot}3\%$. spection of all the results suggests, however, that one could t confidently assert that all reported deviations, as between ferent laboratories, which exceed these limits are due to tematic causes rather than to inherent experimental error. most cases, in fact, it is not possible to find or to give a definite ason for them. Further attempts to increase the accuracy and ecision of measurement, and perhaps also to improve mainte-nce procedures, are thus called for, and are in fact being made various laboratories. Along with these, work is also being ne to improve the reproducibility of the primary standard, rticularly in Germany and in Canada. The particular contri-tion of the N.P.L. to the latter problem will be to study further e accuracy and reproducibility of an alternative method of alizing the unit of luminous intensity with the aid of a thermo-e calibrated absolutely in terms of the N.P.L. radiation scale.[23] is method, in which reference is made to a detector with a own calibration instead of to a primary light source of known ensity, involves much simpler apparatus and methods, but it mains to be seen whether it can give greater consistency.

(6) ACKNOWLEDGMENTS

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WESTERN SUPPLY GROUP: CHAIRMAN'S ADDRESS

By A. H. McQUEEN, Member.

'RESEARCH AND TESTING IN THE SWITCHGEAR INDUSTRY AND ITS EFFECT ON THE SUPPLY INDUSTRY'

(ABSTRACT *of Address delivered at* BRISTOL 17*th October*, 1960.)

The main purpose of my Address is to draw attention to the important part that research has played, and continues to play, in the development of high-voltage switchgear, and to outline briefly some of the more outstanding technical advances which have been made during the past 60 years.

It was in 1904 that H. W. Clothier enunciated the principle of metal-enclosed construction for high-voltage switchgear, the soundness of which can be gauged from the fact that even to this day vast quantities of switchgear are still being made and installed to designs which differ only in detail from those propounded at that time. It was not, however, until 1929, when the first short-circuit testing station was commissioned in Great Britain, that real technical advance was possible. Up to this time, most high-voltage circuit-breakers were of the plain-break type, and, because there was so little performance data to go on, their design was more empirical than scientific. With the advent of testing stations, research began to yield epic results, particularly in respect of arc-control devices; for example, the Deion grid by Slepian, the Wedmore–Whitney arc-control device, and in 1933 the Leeson–Wild arc-sealing turbulator. This work led to substantial improvements in the performance and reliability of high-voltage switchgear and enabled existing frame sizes to be considerably uprated. One of the most important results was the marked effect on selling prices. For example, the manufacturing costs for switchgear of given physical dimensions and general design have, since 1929, increased by about 300%. On the other hand, as a result of improved design and performance, a rating of 11 kV 350 MVA 800 A can now be achieved in switchgear of such reduced physical dimensions that the selling price for switchgear of this particular rating has increased by only 57% over the same period.

Apart from the great benefits to which the arc-control devices gave rise, further research on this subject was greatly stimulated and new methods of approach to the problems of arc control were proposed. The outcome was the development of the air-blast circuit-breaker, which is in use over a wide range of voltages, and of the air-break circuit-breaker, which has rapidly developed from a low-power low-voltage device to one suitable for use on major systems up to 500 MVA and 11 kV. Another major development was the small-oil-volume circuit-breaker, which has found wide application at voltages up to 220 kV, and considerable improvements to high-voltage dead-tank circuit-breakers have enabled them to be successfully used at voltages up to 330 kV.

All these advances were the direct result of research, and amongst other things they show the very great value of short-circuit testing techniques. However, as electrical power systems grew it became increasingly evident that the power available i existing testing stations was inadequate for research, or for proo of performance, and in 1954 a new high-power research statio was built. This new station enabled unit tests to be conducte on circuit-breakers rated at 15 000 MVA and 380 kV.

It will be appreciated that in circuit-breaker research the stud of arc phenomena is of pre-eminent importance. Electromag netic and high-speed cathode-ray oscillographs, pressure gauge speed recorders and the like are, of course, the convention tools of the trade. During the last decade, however, high-spee cine photography has been extensively used and has proved t be a valuable tool in the hands of the researcher. Two importan advantages stem from this:

(*a*) The ability to see the arc, which enables the physics of ar phenomena to be studied in much greater detail.

(*b*) The opportunity to correlate the electrical phenomena (a indicated by the oscillograph records) with what is actually takin place inside the circuit-breaker. Several special cameras have bee developed for this purpose, and some of these are capable of record ing at a rate of 800 000 frames/sec.

Most of this Address is devoted to discussing progress arisin from research on arc control, but it is important to recogniz the great advances which have also been made in the sphere o insulation. The use of resin materials deserves special mentio in this respect. These resin insulators can be produced i complicated shapes to give a better performance than simila built-up insulators, and without expensive machining or fitting The effect is most evident in the smaller and more versatil equipment which has resulted from this new insulation technique

Mention is also made of the considerable research which ha gone into the design of protection and control systems. Moder high-power systems demand high-speed fault clearance, and th new protective systems which are now available have contribute largely to the successful growth of electricity supply.

It will be appreciated that all this research and developmen work is impossible without the use of extensive laborator equipment, and switchgear manufacturers have been at pain to ensure that their equipment is adequate for the purpose.

The Address gives little more than an inkling of what i going on in the larger switchgear manufacturing companies and each item could have been expanded many times over. hope, however, that it has been sufficient to indicate the vas amount of research which is being done, and, indeed, withou which the development of electrical power systems would not b possible. This research is also to the benefit of industr generally, and it should give confidence to those who have t operate circuit-breakers to know that so much thought has bee put into modern switchgear designs, much of it having regard t their welfare and safety.

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A BASIS FOR SHORT-CIRCUIT RATINGS FOR PAPER-INSULATED CABLES UP TO 11kV

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SUMMARY

The various factors which may determine the short-circuit rating of paper-insulated cables are examined, and individual limits are proposed for each which may be decisive. These are the temperature rise of the conductors, the sheath temperature and the peak current. The application of these limits to a range of cables is illustrated.

LIST OF SYMBOLS

A = Cross-sectional area, in^2.
A_c = Conductor cross-sectional area, in^2.
A_s = Sheath cross-sectional area, in^2.
E = Modulus of elasticity, lb/in^2.
F = Electromagnetic force between conductors in a cable, lb/ft, or the axial collapsing force of conductors in a joint, lb.
I = Short-circuit current, kA.
I_1 = Short-circuit current flowing through the sheath, kA.
L = Length of cable, in.
S, S_1, S_2, S_3 = Longitudinal axial stiffness of cable conductors, lb.
f = Frictional resistance to extension of cable cores, lb/in.
f_p, f_s = Tensile stress in belt papers and sheath, lb/in^2.
r_a, r_s = D.C. resistance of armour and sheath at 20° C, ohms.
s = Axial separation of conductors in a multicore cable, in.
t = Duration of short-circuit, sec.
t_p, t_s = Radial thickness of belt papers and sheath, in.
x = Yield of joint at collapsing load, in.
α = Temperature coefficient of resistance, or thermal coefficient of expansion.
θ = Temperature rise, deg C.

(1) INTRODUCTION

A cable may be called upon to carry fault current when already loaded to the maximum temperature permissible for continuous operation. The result is to raise the temperature further. Since faults are infrequent and of short duration, some excess is permissible, but a limit must be observed. This is usually expressed as a maximum conductor temperature. Some accepted values are:

Great Britain[1]	120° C.
Germany[2] ..	Up to 1 kV, 200° C; 20 kV, 170° C; above 20 kV, 140° C.
Germany[3] ..	Up to 3 kV, 160° C; 20 kV, 120° C; above 20 kV, 100° C.
Sweden[4] ..	Up to 11 kV, 200° C.
U.S.A.[5] ..	200° C.

Assuming that all heat dissipated in the conductor is used in raising the conductor temperature, 'short-circuit ratings' are then defined by a formula connecting fault current I kiloamperes, fault duration t seconds, and conductor cross-sectional area A square inches:

$$I = (\text{constant}) \times A/\sqrt{t} \quad . \quad . \quad . \quad . \quad (1)$$

The constant depends on the conductor material, the maximum temperature and the conductor temperature at the start of the fault. For copper conductors limited to 120° C, values of the constant are 86, 63, 47·5, for temperatures of 15° C, 60° C, 85° C at start of fault.

Strict application of the limit of 120° C to the design of cable networks may be onerous, and the limit has been thought unduly conservative. Since the matter is not one on which it was likely that useful quantitative data would be obtained in the course of practical experience, an experimental investigation was put in hand.

Factors entering into the complex problem are:

(*a*) Direct effect of heat on conductor, dielectric and sheath.
(*b*) Direct effect of electromagnetic forces.
(*c*) Effects of heat as displayed through thermal expansion and the forces restraining it.
(*d*) Effects of environment of the cable.
(*e*) Effects of rapid reclosure.

These factors must be considered in relation both to the cable and to the joints, and a factor which is decisive in one case may not be so in another. A thorough study of all factors affecting the short-circuit rating of single-core and belted multicore paper-insulated cables with copper conductors and lead or lead-alloy sheaths, and joints of conventional construction, for voltage ratings up to 11 kV, has produced data which may be used to calculate short-circuit ratings for cables within the range quoted, and a basis on which may be determined similar data covering other cables.

A simple statement of a short-circuit rating, applicable to a cable of given size and construction, may lead to unsatisfactory results if applied indiscriminately. A specific rating in any given situation should be allotted only after full account has been taken of the considerations set out in a companion paper.[13]

Some of the effects considered are influenced by the duration of the fault. The paper is intended mainly to relate to faults of duration not exceeding 3 sec.

(2) DAMAGE TO DIELECTRIC BY HEATING

(2.1) Effects of Heat on Cable Papers

Conductors must not be allowed to reach temperatures at which contiguous insulation is damaged; limiting factors in this respect have been examined.[6, 7, 8] The criterion adopted was that paper was damaged if discoloration (charring) was just perceptible when the paper (without impregnant) was examined in good daylight. Samples of paper removed from cables were laid on an anvil and a heated copper pencil maintained at a known temperature was applied at a suitable pressure. Experi-

The paper is based on Report Ref. F/T195 of the British Electrical and Allied Industries Research Association.

ments with different times of application showed the maximum time which just avoided perceptible discoloration at this temperature. A series of experiments at different temperatures gave the charring characteristic for a given paper. Typical characteristics in the form of curves connecting temperature and time to reach incipient charring are given in Fig. 1. These include

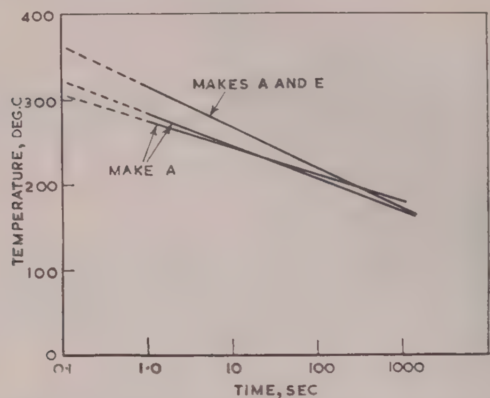

Fig. 1.—Charring characteristics of some cable papers.
Temperatures are those at which paper is just uncharred.

curves relating to the papers encountered which were most and least resistant to charring. Resistance to charring appeared to increase with hardness and density of the paper. It was established[7] that heat treatment which just avoided perceptible charring had negligible effect on the mechanical and electric strength of the paper.

The rate of charring at constant temperature can be defined as the reciprocal of the time taken to reach a given state of decomposition, say the threshold of discoloration. Fig. 2 illustrates the manner in which this rate varies with temperature.

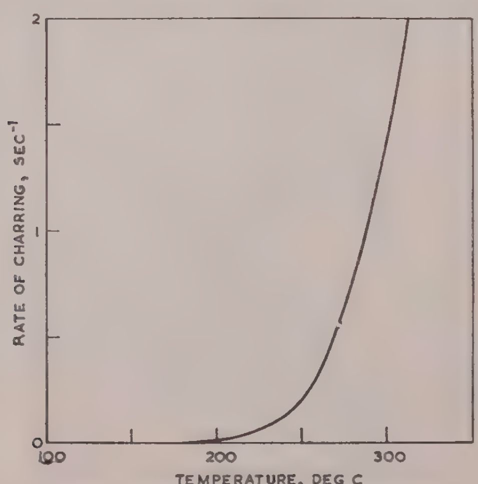

Fig. 2.—Charring rate of paper insulation.

If, now, the temperature history of the conductor during and after a short-circuit is known, the rate of charring of the paper in contact with the conductor at any instant can be deduced. Integration with respect to time will yield a quantity which is proportional to the total extent of charring. A value of unity for the integral indicates a total extent of charring equal to the standard, i.e. the threshold of discoloration. It is thus possible to estimate the peak conductor temperature corresponding to incipient charring.

(2.2) Core Temperature Changes during and after Short-Circuit

Heating Period.—The assumption that all energy dissipated in the conductor during the fault is retained as heat is extreme, and allowance for loss would be advantageous if justified. In tests on new cables with substantial amounts of impregnant amongst the strands, brought to temperatures of 300° C and above in 1 sec, the loss was substantial and up to 20% of the total. At temperatures below 200° C the proportionate loss in such cables was less, probably because of the smaller influence of latent heat. In drained or aged and dry cables, with little or no free interstrand compound, the loss was often trivial and the temperature rise close to that given by the equation

$$\log(\theta\alpha + 1) = K(I/A)^2 t \quad . \quad . \quad . \quad . \quad (2)$$

where K is a constant determined by conductor material.

In assessing ratings which must be valid throughout the life of cables, it is necessary to assume no heat loss. This assumption provides a small margin of safety which increases with the duration of the fault.

Cooling Period.—Fig. 2 shows that charring occurs preponderantly whilst the conductor is near peak temperature. It is therefore sufficient to consider cooling to about half the initial temperature rise. Cooling during this period is influenced mainly by the properties of the conductor and of the insulation, and to a negligible extent by sheath and environment, or by shape and degree of compaction of the stranded conductor. The average

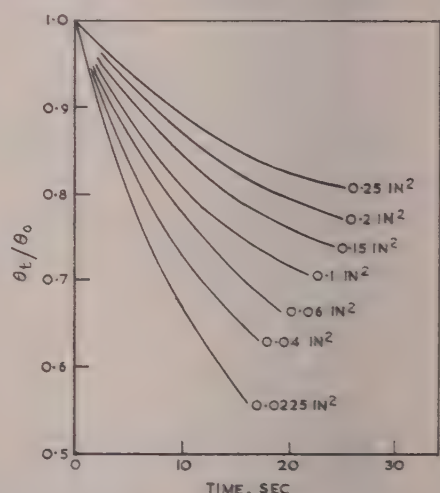

Fig. 3.—Typical cooling curves for copper conductors in cables.

cooling curves of Fig. 3, determined by experiment on cables of various manufactures and types, are in approximate agreement with theory for round solid conductors.

(2.3) Ratings to give Marginal Avoidance of Charring

Short-circuit ratings[7, 8] based on the above information which just avoid charring of the dielectric next to the conductors imply a peak conductor temperature of the order of 270° C, rather more for the smaller conductors which cool rapidly, and rather less for larger conductors which cool slowly. Later work has shown that, in general, other effects impose more stringent limits, so that ratings on this basis are academic but may have application in a few special cases.

(3) CURRENT RATINGS BASED ON CHARACTERISTICS OF SHEATH AND ARMOUR

(3.1) Temperature Limit of Lead Sheath

Short-circuit currents may flow through sheath and armour. The obvious limitation in this respect is that the lead sheath must not melt, but this is quite unrealistic, and critical factors were examined.[9]

Currents were passed through loops composed of the core and sheath of 3 ft and 8 ft specimens of single-core $0\cdot15\,in^2$ 660-volt cable, sheathed with alloy E, without serving or armour. The melting-point of the sheathing alloy was stated by the makers to be 327° C. The current connections to the sheath clamped it tightly to the core, thus, as in service, preventing relative movement. Instantaneous sheath surface temperatures were measured to ±10° C.

If the mean peak sheath temperature reached or exceeded 260° C, the sheath fractured either circumferentially or longitudinally, as shown in Fig. 4. No specimen suffered in an initial test to a temperature less than 260° C, but a specimen which cracked longitudinally in a test to 266° C cracked circumferentially in a further test to 256° C. There is thus a possibility that effects are cumulative. Circumferential cracks are almost certainly due to the longitudinal restraint imposed on the sheath by the core. The temperature rise and the expansion of the latter are trivial. The former expands under compression whilst heating and softening, and contracts under tension whilst cooling and hardening. The end result is longitudinal tensile stress and circumferential fracture. The longitudinal cracks may be associated with hoop stress due to electromagnetic forces between conductor and sheath. The concentration on half of the periphery suggests circumferential variation of wall thickness as a significant factor. Longitudinal variations of thickness may in part explain the fact that peak sheath temperatures at different points varied by ±10° C with 3 ft samples and ±20° C with 8 ft samples in a single test to a mean temperature of the order of 250° C. This may also be due to incomplete screening from draughts.

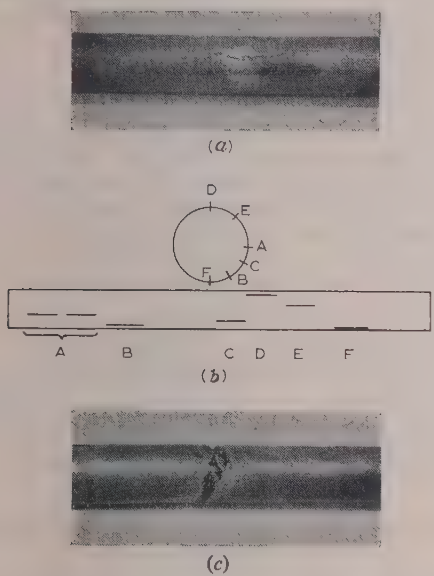

Fig. 4.—Damage to lead sheaths (showing positions of splits).

The above observations were later confirmed by similar tests on lengths of $0\cdot1\,in^2$ 2-core, 1 100-volt cable sheathed with alloy E and with pure lead, of $0\cdot06\,in^2$ cambric-insulated cables, and of other cables, some wire armoured. There was no significant difference in the behaviour of the pure lead and of the alloy sheaths.

One potential cause of failure is the susceptibility of lead sheathing to brittle fracture with reduced elongation under slow tensile strains, a susceptibility which, in general, increases with increase in grain size. If lead or alloy sheathing is heated above some critical temperature, coarse grain structure may result. The sheathing of the $0\cdot1\,in^2$ cables mentioned above was examined in this respect. The grain size was large in the untested pure lead sheaths and no larger in a sample completely split apart in a test to above 250° C. The grain size in the alloy sheaths was small before testing and not significantly larger in a sample tested to 205° C, but was slightly greater in a sample tested to 250° C.

Evidently sheath temperature must be limited to about 250° C if immediate fracture is to be avoided, and to a value between 200° C and 250° C, depending on composition, if the structure of the material is not to be changed.

(3.2) Heat Loss from Sheath during Short-Circuit

During short-circuit the sheaths lose heat to the interior insulation and to the covering or the air. In all cases examined the loss has been sufficient to bring the maximum recorded sheath surface temperature significantly below the value calculated neglecting heat losses. Typical data are presented in Fig. 5. The reduction appears to be roughly proportional to

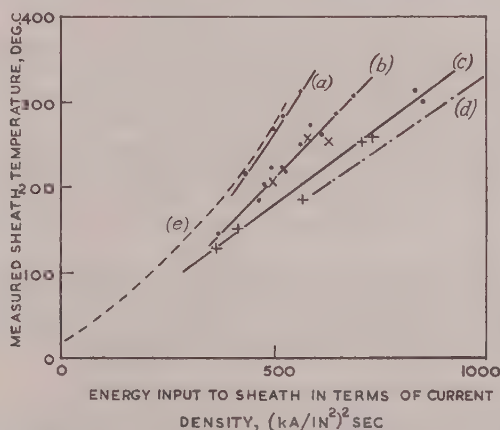

Fig. 5.—Illustrating heat loss from cable sheaths.

(a) $0\cdot022\,5\,in^2$ single-core cable.
(b) $0\cdot15\,in^2$ single-core cable (x = tests of $0\cdot8$ sec duration).
(c) $0\cdot15\,in^2$ 3-core cable.
(d) $0\cdot15\,in^2$ single-core cable, additional samples.
(e) Theoretical relation, assuming no heat loss from sheath.
All durations $0\cdot3$ sec except where shown otherwise.

the ratio sheath-thickness/sheath-diameter and has been as great as 25% for $0\cdot15\,in^2$ 3-core 660-volt cable in air, and as little as 10% for $0\cdot022\,5\,in^2$ single-core cable in air. The loss is approximately doubled for cables embedded in compound (to represent serving) and subjected to short-circuits of 1 sec duration. There is considerable variation between samples of the same design tested in air [curves (b) and (d) of Fig. 5], probably associated with variation of thermal contact as affected by tightness of fit on core.

(3.3) Alleviation of Sheath Current by Armour

Through short-circuit currents in the envelope of an armoured cable will divide between sheath and armour. The system is

complex, since change of temperature affects both the resistance of the sheath and the impedance of the sheath-armour circuit, and the latter depends on the permeability of the armour wire. At the current densities of interest, however, internal magnetic circuits of the armour wires are already saturated and the current division is determined almost entirely by the resistance ratio. This is true[9] even at such a relatively low armour current density as 1 000 amp/in^2.

Table 1 gives some results of tests of 1 sec duration, selected from a large number in which the maximum sheath temperature was of the order of 250° C. There is a small systematic anomaly, as yet unexplained, in that the armour current is always slightly greater than the value deduced from the resistance ratio, whilst all general considerations suggest that it should be slightly less. Nevertheless, the current division is never less favourable to the sheath than the value deduced from the ratio of resistances at 20° C, and it is suggested that this value be adopted in rating calculations.

Table 1

Division of Short-Circuit Current between Sheath and Armour

Cable	0·0225 in² 3-core 660 V 4 ft long	0·1 in² 4-core 660 V 4 ft long	0·1 in² 4-core 660 V 20 ft long	0·1 in² 3-core 11 kV 4 ft long
Total r.m.s. current, kA ..	15·0	31·3	26·0	43·4
Armour r.m.s. current, kA	11·5	24·2	20·0	29·6
Current ratios based on				
1. Calculated d.c. resistance at 20° C	0·69	0·7	0·7	0·63
2. Measurement at peak of 1st half-cycle	0·74	0·73	0·78	0·68
3. R.M.S. value over short-circuit duration	0·77	0·77	0·77	0·68
4. Measurement at peak of last half-cycle	0·8	0·79	0·78	0·71
5. Calculated d.c. resistance at end of short-circuit	0·75	0·8	0·8	0·72

The relief afforded by tape armour is trivial and can be neglected. The case of double wire armour has not been examined experimentally, but calculation of current division from the cold-resistance ratio would be appropriate.

(3.4) Armour Temperature

In all cases examined the temperature rise of the armour has been less than that of the sheath. At temperatures of the order of 250° C, there has been no damage to the armour beyond some loss of compound from the bedding. The temperature limit of the sheath-armour system is thus fixed by that for the sheath. Although the presence of, and heat in, the armour affects the thermal time-constant and cooling curve of the sheath, the temperature rise of the sheath during short-circuit is not significantly increased by heat transfer from the armour.

(3.5) Charring of Outer Cable Papers

No sign of charring has ever been observed in the outer papers of cables, the sheaths of which have been heated to 250° C in 1 sec. The considerations of Section 2 show that in the most unfavourable case (least resistant paper, slow cooling) incipient charring may occur at a sheath temperature of 270° C. Since the limit proposed is lower, there is no danger from this cause.

(3.6) Current-Carrying Capacity of Armour Bonds

Well-made armour bonds, using malleable iron clamps, have shown no signs of distress when passing currents of the order contemplated. Meticulous assembly is, however, necessary if the most favourable current division between armour and sheath is to be maintained.

(3.7) Determination of Limit

Sheath temperatures must not be allowed to exceed 250° C. Important damage may occur just above that temperature, and the peak temperature should be lower by a margin of safety. Heat loss during short-circuit will cause the real temperature rise to be less than that calculated on a basis of total energy absorption, by a margin which will always be more than 10% and increases with diameter of the sheath. It is suggested that sheath short-circuit ratings be calculated on the basis of total energy absorption, a sheath temperature limit of 250° C, and division of current between sheath and wire armour (if any) as deduced from their relative resistances at 20° C. The heat losses neglected then provide the necessary margin of safety. This course avoids the possibility of damage to the outer cable papers or important changes in the structure of the sheathing material. The presence of tape armour should be ignored.

To assess the short-circuit current limit, it is necessary to define the sheath temperature at the start of short-circuit. This should be taken as that under normal full-load conditions. It may be as low as 40° C for higher-voltage cables in soil of low thermal resistivity and as high as 75° C for lower-voltage cables in unfavourable environments. Taking an average figure of 60° C, the sheath current limit, I_1 kiloamperes, for unarmoured and tape-armoured cables is then

$$I_1 = 19A_s/\sqrt{t} \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

For wire-armoured cables the permissible short-circuit current in the sheath and armour is $(r_a + r_s)/r_a$ times as great. For single wire-armoured cables to B.S. 480: 1954 of about ½ in diameter over sheath, $(r_a + r_s)/r_a \simeq 3$, and for cables of about 3 in diameter over sheath, $\simeq 2$. Caution is required when armour may be subject to corrosion.

(4) BURSTING OF CABLES BY ELECTROMAGNETIC FORCES

(4.1) Tests

The heat developed in cables by faults which are very quickly cleared ($\sim$0·2 sec) may not be damaging at currents at which there is danger that electromagnetic forces may burst the envelope.

Tests were made[9] on 10 ft specimens of 660-volt 0·25 in^2 3-core cable to B.S. 480: 1942 fitted with normal end boxes plumbed to the sheaths. The cables were lightly restrained along their length by a rope binding, and the end boxes were firmly clamped to the test gantry. The sheaths of some specimens were removed from all but 6 in adjacent to each end box. The results are presented in Table 2.

The cables without lead sheath either burst during the test or were of unchanged external appearance after it. With sheathed cables an intermediate stage was observed, in which the sheath became deformed to a hexagonal shape. A repeat test on a cable so deformed, but not by the highest current which could so deform it without bursting, did not appreciably increase the deformation, which was not accompanied by other signs of damage. Without sheaths the cable burst at 78 kA (peak) and was undamaged at 68 kA (peak). The cables with sheaths burst at 95 kA (peak), were deformed at 83 kA (peak) and appeared undamaged at 72 kA (peak).

Table 2

RESULTS OF CABLE BURSTING TESTS

0·25 in² Shaped Conductor 660-Volt P.L.Y. Belted Cable to B.S. 480: 1942

Test No.	Specimen No.	Sheathed or not	Initial peak current*	Equivalent symmetrical 3-phase r.m.s. current (0·15 p.f.)	Damage
			kA	kA	
72	2.1.1	Yes	72·4	31·4	None
73	2.1.1	Yes	95·0	41·4	Burst
76	2.1.2	Yes	83·0	36·1	Deformed
79	2.1.3	Yes	95·0	41·4	Burst
80	2.1.4	Yes	75·0	32·6	Deformed
81	2.1.4	Yes	78·0	34·0	Deformed
74	2.2.1	No	51·0	22·2	None
75	2.2.1	No	70·0	30·5	Burst
77	2.2.2	No	65·0	28·3	None
78	2.2.3	No	73·0	31·8	Burst

* For the phase having the highest initial peak.

(4.2) Analysis

To permit generalization on the above results some analysis is necessary. This is most conveniently done in terms of symmetrical r.m.s. current, introducing the peak value through short-circuit power factor, here taken as 0·15, the lowest assumed in assessing circuit-breaker duty in the voltage range in question.

The maximum force F developed on any conductor of a 3-core cable during an asymmetrical 3-phase short-circuit of I kiloamperes (r.m.s.) symmetrical and 0·15 power factor is

$$F = \frac{2\cdot 5I^2}{s} \text{ pounds per foot run} \quad . \quad . \quad . \quad (4)$$

Until the cable bursts, this force must be sustained by the envelope, i.e. belt papers and sheath in the case of unarmoured cable. If the sheath is a tight fit over the belt papers, the strain in sheath and belt will be nearly the same up to the point at which either fails.

Typical stress/strain diagrams for belt papers and sheath material are given in Fig. 6. Although the sheath reaches its maximum stress at a lower strain than does the belt, the sheath is capable of plastic yield at stresses near the maximum, and will continue to stretch until the belt material reaches its ultimate tensile strength. At this point the belt tears apart, the load is transferred to the sheath, and continued thrust bursts the cable.

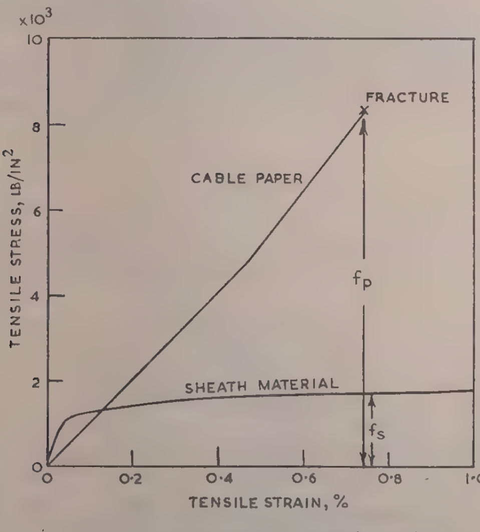

Fig. 6.—Mechanical properties of belt and sheath materials.
f_p = Stress in belt at failure.
f_s = Stress in sheath when belt fails.

The total tensile force in belt and sheath at fracture is $12(f_pt_p + f_st_s)$ lb/ft run. Experimental values for f_p vary from 4 700 to 10 000 lb/in²: values given in B.S. 698: 1936 range from 6 000 to 10 000 lb/in². An average of 8 000 lb/in² has been used below. The correct value of f_s is that developed when the belt material is about to fracture, i.e. at a strain of the order 0·7%. Experimental values for alloy E when rapidly strained at ~20° C are near 2 000 lb/in².

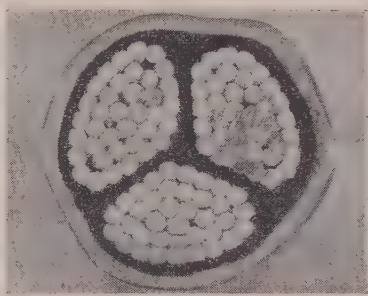

Fig. 7.—Hexagonal deformation of 0·25 in² 660-volt p.l. cable.

If the cable deforms to a hexagonal shape prior to bursting (see Fig. 7) the tension in the envelope is $F/\sqrt{3}$ lb/ft run, and

$$12(f_pt_p + f_st_s) = F/\sqrt{3} \quad . \quad . \quad . \quad . \quad . \quad (5)$$

From eqns. (4) and (5), the symmetrical current level at which the cable may burst at the first asymmetrical peak is

$$I = 2\cdot 89\sqrt{[s(f_pt_p + f_st_s)]} \text{ kiloamperes (r.m.s.)} \quad . \quad . \quad (6)$$

If the sheath fits very loosely over the belt or is absent, an approximation to the bursting current is given by writing $t_s = 0$.

Substitution for f_p and t_p in the above formula gives the theoretical bursting currents for 3-core 0·25 in² 660-volt cable as 39 kA with tight sheath and 28 kA with sheath loose or removed. Corresponding peak asymmetrical values at 0·15 p.f. are 89 kA and 64 kA, which agree well with the experimental results quoted above.

(4.3) Determination of Limit

A short-circuit rating must be established at a safe but reasonably large fraction of the calculated bursting current. It is suggested that this be taken as $1/\sqrt{2}$. The safe short-circuit current is then

$$I = 2\cdot 04\sqrt{[s(f_pt_p + f_st_s)]} \text{ kiloamperes (r.m.s.)} \quad . \quad (7)$$

In practice there will usually exist a hidden factor of safety in that the actual power factor will be greater than 0·15, and thus the maximum initial peak current will be a lower multiple of the symmetrical r.m.s. current than is here assumed.

This limit, which need not be applied in the case of cables protected by h.r.c. fuses, will usually be decisive only in the case of cables of large conductor area. Fig. 8 illustrates its application to a 0·3 in² 11 kV cable and 0·5 in² 1 100-volt cable. For the former the limit exceeds the short-circuit current range covered by oil circuit-breakers to B.S. 116.

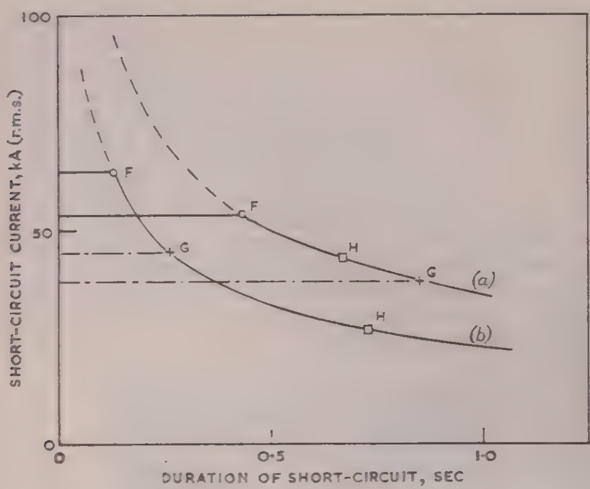

Fig. 8.—Short-circuit current limitation due to bursting.

(a) 0·5 in² 1 100-volt: $I\sqrt{t} = 70$ kA/in².
(b) 0·3 in² 11 kV: $I\sqrt{t} = 76{\cdot}5$ kA/in².

Curves represent limit due to joint damage.
Horizontal line through F represents the theoretical bursting current (independent of duration).
Horizontal line through G represents the suggested limit to avoid bursting (=0·7F).
H represents the maximum current of a standard-size oil circuit-breaker.

(5) LIMITS DUE TO LONGITUDINAL EXPANSION OF MULTI-CORE CABLES

(5.1) Analysis

When conductors of a cable are heated they exert a force on any member restraining expansion. In particular, joints in multicore cable seem inherently weaker than the conductors themselves and may yield in compression when the cable is heated.[10] Preliminary tests on 40 ft and 100 ft lengths of 0·15 in² 1 100-volt 4-core p.l. cable, each including a joint, showed that joints might suffer damage at conductor temperatures below the limit set by other considerations. The type of damage in question is illustrated in Fig. 9.

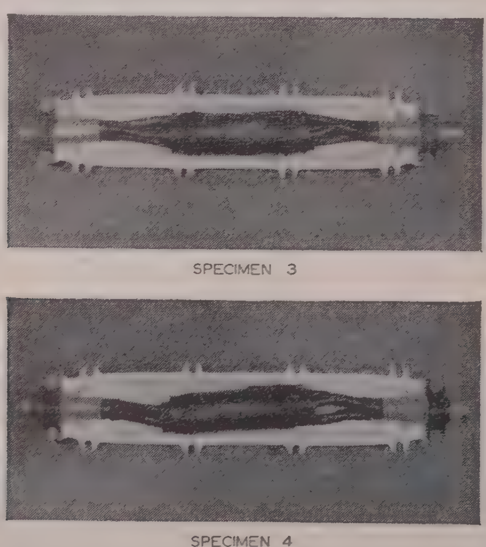

Fig. 9.—Straight-through joints after short-circuit tests.

Joints.—A multicore joint embedded in compound, subjected to compressive forces applied along the axis of the conductors at the entry, behaves first as an elastic member, with yield proportional to applied thrust, and then collapses by buckling of the conductors, with or without bursting of any tape binders fitted. Fig. 10 shows the thrust/yield diagram for such a joint,

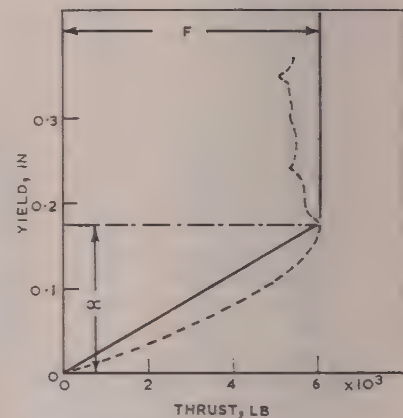

Fig. 10.—Thrust/yield diagram for a 0·3 in² 1 100-volt joint in compression.

---- Actual result.
—— Idealized result.

which may be regarded as having the characteristics represented by the full line and defined by the quantities

$F =$ collapsing load applied to the three conductors, lb
$x =$ yield of joint at point of collapse, in.

Any lengthening of the joint itself due to heating may be neglected.

Cables.—The conductors and core insulation may be regarded as a long spring bound by friction to the belt and sheath. The stiffness, S, of such a spring, the frictional forces and the coefficient of thermal expansion, α, of the conductor material, together with length, determine its performance. The laws governing friction are complex, but here is assumed a uniform frictional resistance, f, to relative motion. When the sheath of the cable is kept straight, stiffness is a property of the conductors and core insulation alone; otherwise it is affected by the properties of sheath, armour and environment.

Fig. 11 shows the result of a compression test on a 9 ft length

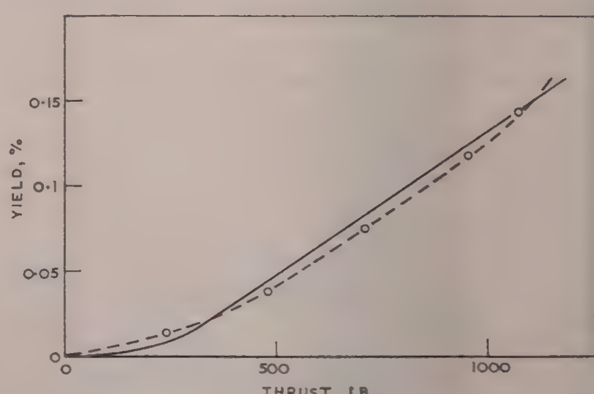

Fig. 11.—Thrust/yield diagram on cores of a 9 ft length of 0·022 5 in² 3-core 1 100-volt cable.

---- Actual result.
—— Idealized result.

Table 3

DETAILS OF JOINTS TESTED IN COMPRESSION

Voltage	Type of joint	Source	Specimen No.	Size	Free length L	Spacing at ferrules, D	Box or lead sleeve, i.d.	Length of binder, B	Compound
				in²	in	in	in		
1 100 V			5		6·5	1·125	—	*	None
	Box		6		—	—	—		None
			11	0·06	8·5	1	3		
			12†		8	1	3		B
	Sleeve		9		9	0·875	1·5		
	Sleeve		10‡		9·5	1	1·5		
		A	1		8·5	—	—		None
	Box		2		8·5	—	—		None
			14		10·5	1·5	3·25		
			15	0·15	10·75	1·5	3·25		
			7		13	1	2·25		B
	Sleeve		8		15	1	2·25		
			13		14	1·25	2·25		
	Box	A	29	0·15	9	—	3·25		None
	Sleeve	B	27§	0·2	18·2	1·5	4·75		B
	Sleeve	B	28§		17·7	1·6	4·75		
	Box		16	0·3	10·5	1·875	4		B
	Sleeve	A	17		18·5	1·875	3·25		
	Sleeve		18		18·5	1·875	3·25		
				in²	in	in	in	in	
11 kV			19	0·022 5	17·3	1·3	3·25	8·5	
			20	0·022 5	17·7	1·3	3·25	8·5	
			3	0·06	17·3	1·4	3·25	8·5	
			4	0·06	14·5	1·4	3·25	8·5	
		A	21		19	1·4	3·52	9·2	
	Sleeve		22	0·15	18·7	1·4	3·52	9·2	A
			23		19·1	1·4	3·52	9·2	
			24		20·6	1·5	4	10	
			25	0·25	20·7	1·5	4	10	
		B	26		18·6	1·5	5	7	

The 11 kV joint B was constructed with two short star-shaped porcelain spacers and paper tubes: all joints A had long pre-impregnated paper spacers and wrappers.
* The length B on 1 100-volt joints was equal to the tape width, i.e. nominally 1 in.
† Unbound.
‡ Touching sleeve.
§ Four-core joints.
For the significance of L, D and B see Fig. 12.

of cable with sheath held straight and unchanged in length. The full line represents the performance of an ideal system of fixed stiffness, length and frictional resistance, which is assumed to represent the cable.

Combined Cable and Joint.—To damage the joint, the cable must exert a force F pounds, and the cores at each end of this joint must move inwards through $x/2$ inches. If a joint between two lengths of cable is not to be damaged, the temperature rise of the cores above the temperature at which the system is stress-free must not exceed

$$\theta = \frac{F + \sqrt{(Sxf)}}{\alpha S} \quad \deg C \quad . \quad . \quad . \quad . \quad (8)$$

This formula holds only if the total length of cable involved exceeds $2\sqrt{(Sx/f)}$ inch per yielding joint. In practice most cables will be longer.

(5.2) Strength of Joints

Joints in 1 100-volt and in 11 kV cable have been subjected to axial compressive forces, the force and yield being simultaneously measured. The rate of load increase was about 20–70 lb/sec, depending on conductor size. The joints were made up in the establishments of two separate members of E.R.A. The 1 100-volt joints included samples suitable for lead sleeve and for cast-iron box joints; the 11 kV joints were all suitable for sleeves. The conductors were plumbed into a common ferrule about 1 ft from each end of the joint. Dimensions and other characteristics of these joints are given in Table 3.

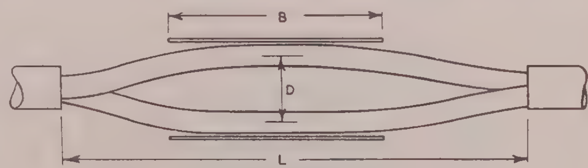

Fig. 12.—Dimensions referred to in Table 3.

Most joints were filled with hard-setting compound, but currents sufficient to bring the conductors to 60–70° C were passed for about half an hour before the test, so that the compound in the joint was more or less in the condition to be expected in service. One or two joints in hard-setting compound and one with no compound were tested at room temperature.

Measured values of F are given in Fig. 13 (for 11 kV joints) and Fig. 14 (for 1 100 V joints). When a joint had been taken beyond the knee of the force/yield curve, it had permanently buckled; below the knee, joints behaved more or less as elastic members.

The 11 kV joints from source A gave quite consistent results, the strength of every warm joint tested being close to 8 000 lb/in². When cold, a joint was about 50% stronger than when warm. The joint with porcelain spacer (source B) was about 30% weaker than the corresponding joint with pre-impregnated paper spacer, possibly because the former allows or imposes more abrupt change of curvature.

The 1 100-volt joints were more variable. Like 11 kV joints, they were stronger cold than warm. Although the best from source A were as strong as, or stronger than, the corresponding 11 kV joints, some were weaker. Of the 0·06 and 0·15 in² joints the weakest failed by breakage of the linen-tape binder. One

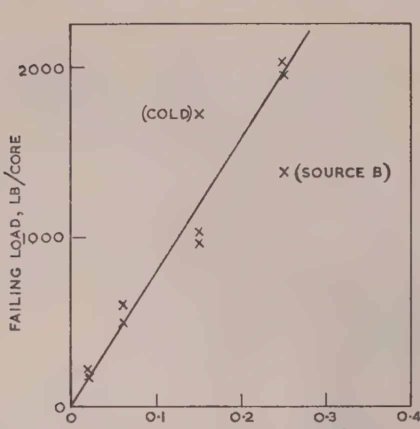

Fig. 13.—Failing load of 11 kV joints.
All from source A except where shown.

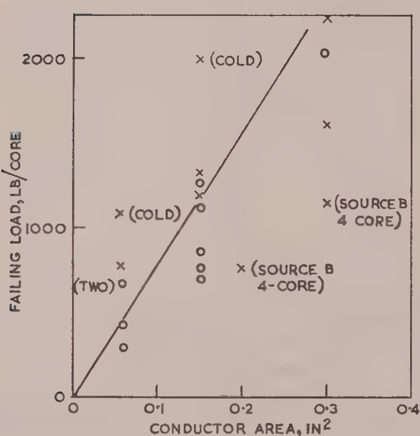

Fig. 14.—Failing load of 1 100-volt joints.
○ Cast-iron box type.
× Lead-sleeve type.
All source A except where shown.

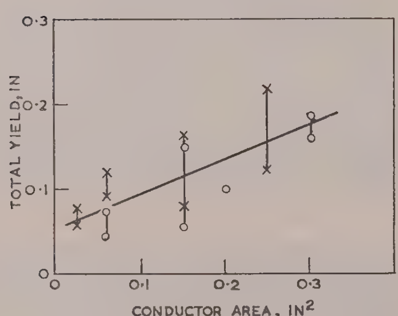

Fig. 15.—Joint yield and conductor size.
Range for 1 100-volt joints.
Range for 11 kV joints.

$0{\cdot}3\,\text{in}^2$ joint, however, failed with the binding intact. The sleeve and box-type joints did not differ significantly in performance, the greater length of the former perhaps being compensated by smaller conductor spacing. The 4-core joints from source B were consistently weaker than would be expected from the behaviour of the 3-core joints from source A.

Fig. 15 shows how the yield of a joint at failure varies with conductor size. The results are variable but can be taken as represented by the straight line.

The more constant strength of the higher-voltage joints may result from the presence of the spacer incorporated therein. There seems no reason why joints for cables for voltages lower than 11 kV should not be as strong as those for 11 kV cables, and it is probable that, with minor changes in practice, joints at all voltages up to and including 11 kV could be constructed to have a strength of 8 000 lb/in² of total conductor section.

(5.3) Longitudinal Stiffness of Conductors

(5.3.1) Tests by Compression of Cold Cable.

Samples were tested in an apparatus in which an axial thrust was applied to the conductors whilst the cable was held straight. The sheath was restrained by friction with the member holding the cable straight. The stress/yield diagrams usually approximated to the ideal form illustrated in Fig. 11. Fig. 16 presents

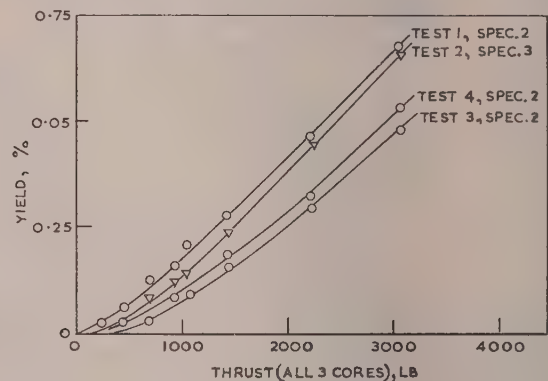

Fig. 16.—Load compression curves for 1 100-volt $0{\cdot}15\,\text{in}^2$ cable.

typical examples. The stiffness of the cable has in every case been taken as the best straight line through the substantially linear portion.

The stiffness was almost always less in a first test than in subsequent tests on the same sample. The low initial stiffness was recovered after a rest of 24 hours. The effect was most marked for the $0{\cdot}0225\,\text{in}^2$ cable, which had round conductors, but for larger conductors (all shaped) it did not change significantly with size. If the cable was stressed repeatedly to a moderate value the low stiffness reappeared on the first application of increased load. This effect has not yet been explained.

Approximate values for the effective modulus of elasticity of the conductor material, as derived from these tests, are given in Tables 4 and 5. The values are variable: for 1 100-volt and 11 kV cables it appears reasonable to use 10×10^6 and $7 \times 10^6\,\text{lb/in}^2$, respectively, as the effective moduli on a first application of thrust, each being an approximate average excluding the results for the smallest conductor.

(5.3.2) Tests by Restraint of Expansion on Heating.

To check that the simple method of test described above was applicable to the restraint of expansion by heating, tests were made on $0{\cdot}15\,\text{in}^2$ 3-core cable by two other methods. In the first set on 1 100-volt cable, the test machine in which the cable was placed was adjusted so that the thrust applied was just above zero. Current was then passed through the three conductors to raise their temperature by 30–50° C in times of the order of 3 min. The cable was partially prevented from expand-

Table 4

Apparent Modulus of Elasticity of 1 100-Volt Cable Conductors Tested Cold in Compression

1	2	3	4	5	6	7
Size	Specimen No.	Test No.	Modulus of elasticity		Ratio: col. 4/col. 5	Load range
			First test of series	Average of subsequent tests		
in²			lb/in² × 10⁶	lb/in² × 10⁶		lb
0·022 5	11	1	5·35		0·57	475–701
		2–9		9·2		
		10	4·9			
		11–15		10·8	0·45	701–1 060
0·06	7	1	11·7			
	7	2–3		15·1	0·78	1 445–3 025
	8	1	13·3			
	8	2–5		16·1	0·83	1 445–3 025
0·15	1	1	10·3	—	1·03	1 445–3 025
	1	2–5	—	10		
	2	1	9·2			
	2	2–4, 10–11		9·9	0·93	1 445–3 025
	3	1	7·5			
	3	2–3		9·4	0·8	1 445–3 025
0·2*	1	1	8·36			
	1	2		9·5	0·88	1 915–3 200
0·3	18	1	10·4			
	18	2		12·6	0·83	2 210–6 370
0·15 (single core in three-core cable)						
	10	1	10·7	—		
	10	2–7		12·4	0·86	701–1 060
	10	9	11·9		0·95	1 060–2 220
	10	10–14	—	12·5		

* All cables were 3-core p.l.y. unserved except that marked, which was 4-core p.l.y.s.w.

Table 5

Apparent Modulus of Elasticity of 11 kV Cable Conductors Tested Cold in Compression

1	2	3	4	5	6	7
Size	Specimen No.	Test No.	Modulus of elasticity		Ratio: col. 4/col. 5	Load range
			First test of series	Average of subsequent tests		
in²			lb/in² × 10⁶	lb/in² × 10⁶		lb
0·022 5	21	1	9·5	—		500–1 200
		4	—	15·7	0·6	
0·06	22	1	6·9	—		
		5	—	9·0	0·77	800–2 800
0·15	23	1	4·2	—		500–3 500
		5	—	9·14	0·46	
	25	1	8·4			250–3 740
0·25	24	1	7·44			1 000–6 750

ng by the machine which recorded the resulting thrust and expansion. In the second set, on 11 kV cable, thrust was so applied that the expansion remained zero, and the effects of friction were eliminated. In all these tests, the cables exhibited stiffness, and thus an effective modulus of elasticity, which was about 70% of that measured on first compression of the cold cable.

It was concluded that the effective stiffness of conductors in the circumstances of present interest is about 70% of that measured in a compression test on a cold cable stressed for the first time after a rest of at least 24 hours. The effective modulus E_1 for the copper cores of 1 100-volt cable becomes 7×10^6 lb/in², and of 11 kV cables, 5×10^6 lb/in².

(5.4) **Friction between Cores and Sheath**

The frictional resistance between cores and sheath was assessed by compressing the cable in the machine with a second thrust-measuring cell at the end remote from that at which the thrust was applied. The difference in applied loads, with thrust increasing and decreasing, for a given load at the remote measuring cell, indicated twice the frictional resistance for the whole cable. Some results are given in Fig. 17. They are

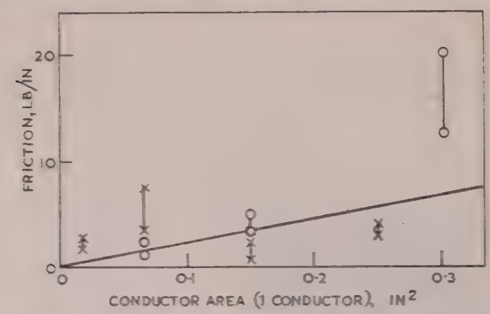

Fig. 17.—Variation of friction with conductor size.

Range for 1 100-volt cables.

Range for 11 kV cables.

extremely variable but are reasonably well represented by crediting the cables with frictional resistance of 7 lb per inch run per square inch of total conductor area.

(5.5) Temperature at which Cable in Service is Free of Longitudinal Mechanical Stress

If a short-circuit current limit is to ensure that the compressive force in the conductor-joint system does not exceed some predetermined value, its application requires assessment of the conditions in which the system is stress-free. The system when newly installed should be stress-free at ambient temperature, but if normal operation imposes a daily cycle of temperature and stress, the system may slowly accommodate itself and reach a state in which it is free of stress at about the mean of the temperature range. To examine this, a 100 ft length of $0 \cdot 15\,\mathrm{in}^2$ 3-core 11 kV p.l.y.s.w.s. cable was installed in a rigid environment with facilities for measuring conductor temperature and longitudinal thrust at one end box. The longitudinal thrust was initially zero at room temperature. The cable was then subjected to a repetitive load cycle of 8 hours on and 16 hours off, with a conductor temperature rise of 40° C. The cable gradually accepted a longitudinal permanent set such that it was stress-free at a temperature rise of about 20° C.

(5.6) Longitudinal Stiffness of Armour and Lead

In some circumstances the effective longitudinal stiffness of cable conductors as seen from two joint boxes depends on the longitudinal stiffness of the envelope (sheath or sheath and armour) which connects the boxes.

To provide data on armour, tests were made on 9 ft lengths of wire-armoured cables with outside diameters of 2·1, 2·25, 2·53 in. The armouring, positioned normally on the cable, was clamped in conventional armour clamps and tensile load was applied, the system being restrained from twisting. The results are sufficiently well represented by crediting the armouring with an effective modulus of elasticity, E, of $5 \times 10^6\,\mathrm{lb/in}^2$.

After a study of available data and some experiments with rates of strain appropriate to the present tests, it is proposed in the present context to allot to lead sheathing the value $E = 1 \cdot 7 \times 10^6\,\mathrm{lb/in}^2$.

(5.7) Longitudinal Stiffness due to Environment

If a cable is laid in an unyielding straight tube between joint boxes which are fixed in position, the apparent stiffness of the system to thrust applied between core and end boxes is S_1, the stiffness of the conductors discussed in Section 5.3. If the tube is straight and unyielding but the joints are free to move in relation to the tube, a thrust so applied puts the envelope into tension and the effective stiffness of the system is $S_1S_2/(S_1 + S_2)$ lb, where S_2 is the stiffness of the envelope in tension. S_2 can be evaluated from dimensions using Section 5.6. This also is the stiffness appropriate to the case when the joints are at fixed separation but the cable between them is unconstrained.

If the cable is embedded in a resistant environment, as, for example, the earth, a third component of longitudinal stiffness, S_3, enters. This may be regarded as the effective stiffness of an incompressible but perfectly flexible rod, of the same dimensions as the cable, laid in a similar medium and following a similar track. The stiffness of the cable laid in a medium to a thrust between cores and the sheath system is then

$$S_1(S_2 + S_3)/(S_1 + S_2 + S_3) \quad \text{pounds} \quad . \quad . \quad . \quad (9)$$

The value of S_3 depends, not only on the dimensions of the cable and the properties of the medium, but also on the deviation of the cable axis from a straight line between joints.

For conditions likely to be frequently met in practice with buried cables (cable offset from straight line by not more than one-half of the cable diameter; wavelength of deviation 20 times the cable diameter; fairly hard soil), S_3 is very large indeed compared with S_1 and S_2. There is thus no relief of stress by movement of the cable in its environment, and the effective stiffness of a cable laid direct is to be taken as S_1.

(5.8) Permissible Temperature Rise

From the above may be calculated permissible temperature rises for armoured cables in ducts or air with no important lateral constraint, and for armoured and unarmoured cable laid direct. These are given in Tables 6 and 7 respectively.

Table 6

PERMISSIBLE TEMPERATURE RISE FOR WIRE-ARMOURED CABLES TO B.S. 480: 1954 IN AIR

Cable size	Temperature rise	
	1 100 V	11 kV
in²	deg C	deg C
0·06	160	155
0·1	190	180
0·15	230	200
0·3	270	260

Table 7

PERMISSIBLE TEMPERATURE RISE FOR P.L.Y. CABLES TO B.S. 480: 1954 BURIED DIRECT IN SOIL

Cable size	Temperature rise	
	1 100 V	11 kV
in²	deg C	deg C
0·06	85	115
0·1	85	115
0·15	90	120
0·3	90	125

The limits increase with conductor size mainly because with the larger conductors the cores must move further into the joint before it collapses.

The temperature rises for armoured cables in air are greater than those for the corresponding cables laid direct because the

effective stiffness of the conductors viewed from the joint box is less in the former case. Buckling of joints is unlikely to set a limit to the permissible temperature rise of unarmoured cables in ducts or in air, except for small (0·06 in² or less) cables in air, cleated at very frequent intervals and improbably straight between cleats.

(5.9) Experimental Confirmation

The validity of the above analysis has been generally confirmed by test. The agreement is best illustrated by the results of two sets of tests made when all the factors involved were understood and arrangements could be made to illustrate their individual effects to the extent possible.

The cables tested where 100 ft lengths of 3-core 0·15 in² 11 kV belted p.l.y. each with a joint at mid-length. The cables were laid in the middle of three 3½ in o.d. steel pipes in trefoil, which with the joint boxes were clamped to the test gantry. The two end trifurcating boxes were mechanically connected through a load-measuring cell.

The conductors were maintained near working temperature for 30–60 min before each test by loading current. So that the conductors might be in a determinate state of stress, the end boxes were adjusted when the cable was cold before each test, so that the total tensile force in the pipes joining them was about 500 lb. The conductors were additionally stressed prior to the start of each short-circuit, by restraint of the expansion due to preheating. Successive tests on a single sample were made at intervals of at least one day.

The leading data relating to the tests are set out in Table 8, which includes a column relating to the 'average' values for the size of cable in question as determined by the above analysis. The temperature rises causing damage to the joints (92–107° C) agree well with the values calculated from the measured or deduced characteristics of the system tested. Each is rather less than the value calculated from 'average' characteristics, because the test cables were stiffer, and the joints weaker, than the average. The difference does not invalidate the conclusion that the limit based on average values should be acceptable, especially if this is coupled with a recommendation that strict attention be paid to the mechanical strength of the joints. The environment in these tests was extremely rigid.

The damage to the joints on both specimens is shown in Fig. 9.

(5.10) Determination of Limit

It is suggested that the limits set out in Table 7 be accepted as defining permissible short-circuit duties for 11 kV belted multi-core cables as concerns currents in the conductor, and that 120° C rise above the stress-free temperature is a suitable mean figure for such cables of all sizes. It is further suggested that for these cables the stress-free temperature be taken as 40° C. It is necessary to realize that the conductor temperature at start of short-circuit may be the maximum normal operating temperature, namely 65° C, so that the available short-circuit temperature rise is 95° C.

The permissible short-circuit level is then

$$I = 76{\cdot}5A_c/\sqrt{t} \quad \text{kiloamperes} \quad . \quad . \quad . \quad (10)$$

This limit leads to a nominal maximum conductor temperature of 160° C.

The temperature rises permissible for 1 100-volt cables are lower, of the order of 90° C. The stress-free temperature of such cables fully loaded cyclically will be of the order of 50° C, resulting in a maximum permissible temperature of 140° C. It is considered, however, that some allowance should be made for the fact that, at the current densities in question, the voltage drop along the cables will be of the order of 1–3 volts/yd, and fault currents in 1 100-volt cables usually substantially less than those calculated from fault MVA at the terminals. It would be convenient to work to the same upper limit (160° C) as for 11 kV cables: taking account of the higher normal operating temperatures of 1 100-volt cables, the short-circuit duty permissible is defined by

$$I = 70A_c/\sqrt{t} \quad \text{kiloamperes} \quad . \quad . \quad . \quad . \quad (11)$$

Table 8

RESULTS OF SHORT-CIRCUIT TESTS ON 100 FT LENGTHS OF 0·15 IN² 11 KV 3-CORE BELTED CABLE WITH STRAIGHT-THROUGH JOINTS

1. Specimen No.	3	3	4	'average'
2. Measured modulus of elasticity, lb/in²	$7{\cdot}2 \times 10^6$		$8{\cdot}3 \times 10^6$	7×10^6
3. Effective modulus (0·7 × row 2), lb/in²	5×10^6		$5{\cdot}8 \times 10^6$	$4{\cdot}9 \times 10^6$
4. Effective modulus (from heating tests), lb/in²	—		$6{\cdot}2 \times 10^6$	—
5. Frictional resistance, lb/in²	4		2·5	3·1
6. Test No.	17 103	17 104	19 747	—
7. Cold temperature, deg C	20	20	18	—
8. End thrust when cold, lb	380	380	300	—
9. Stress-free temperatures, deg C	10	10	12	—
10. Temperature at start of short-circuit, deg C	53	40	38·5	—
11. End-thrust at start of short-circuit, lb	1 650	1 145	1 250	—
12. End-thrust at end of short-circuit, lb	4 700	4 390	3 880	—
13. Short-circuit current (mean r.m.s.), kA	11·2	12·3	11·5	—
14. Short circuit duration, sec	0·84	0·87	0·84	—
15. Final temperature (total energy absorption), deg C	127	127	114	—
16. Final temperature (corrected for loss), deg C	117	117	104	—
17. Temperature rise above stress-free temperature, deg C	107	107	92	—
18. Friction drop along cable, lb	1 550	1 550	1 300	—
19. Final thrust at joint, lb	3 150	2 850	2 580	3 600
20. Joint yield, in	—	—	—	0·11
21. Damage?	No	Yes	Yes	—
22. Calculated temperature rise to cause damage, deg C				120

Adoption of this limit requires strict attention to the strength of joints, particularly near the point of supply. Higher limits are possible for cables in air.

Acceptance of these limits implies that joints will in general be stressed almost to failure, with no explicit margin of safety. There is a small hidden margin in that no account is taken of heat loss during short-circuit, and in many cases in the possibility of stress relief by movement of cable in its environment. Nevertheless, since the limits are determined from average values of characteristics which are rather variable, there will be occasions on which cable circuits operated to these limits may be damaged. The damage will, however, be confined to the joints, and not destructive to the installation as a whole: the risk will probably therefore be acceptable.

(6) POSSIBLE DAMAGE TO DIELECTRIC BY CRUSHING FORCES

During the above work it was realized that the dielectric of fully restrained cable was subjected to considerable crushing forces. To check whether these damaged the cable, measurements of discharge inception voltage[9] were accordingly made on the cables used for the tests of Section 5.9, before and after test. Table 9 gives the history of, and Fig. 18 the discharge inception measurements on, one specimen.

Table 9

SCHEDULE OF DISCHARGE-INCEPTION VOLTAGE MEASUREMENTS ON SPECIMEN 4 OF TABLE 8

Operation	Date and reference
Installation and preliminary tests	
Heat cycling started (4 hours on, 4 hours off)	13.3.57–15.4.57
Discharge-inception voltage tests (Nos. 1–9) ..	21.3.57–15.4.57
Short-circuit test No. 17947 (joint damaged) ..	17.4.57
Discharge-inception voltage test	17.4.57, No. 10
Cable off load	17.4.57–23.4.57
Discharge-inception voltage test	23.4.57, No. 11
Cable on load cycling (4 hours on, 4 hours off)	23.4.57–1.5.57
Discharge-inception voltage test	24.4.57, No. 12
Discharge-inception voltage tests (Nos. 13 and 14)	1.5.57

The pattern of discharge characteristics before the short-circuit did not repeat itself closely, the inception voltage varying over a range up to 3 kV for discharges of a given magnitude. The variation could not be correlated with time or with epoch during cycling. This suggests that the void structure within the cable was continually altering, as, for example, by movement of compound with temperature change. The observations after short-circuit fell almost entirely within the band covered by those before. In the 'blue' phase of the specimen to which Fig. 18 relates, a trivial indication of deterioration appeared 8 days after short-circuit, when discharges of 10 pC* appeared at a lower voltage, but the characteristic so deteriorated is not inferior to that for the 'yellow' phase before test. The display suggested that the number of discharges present at any level was slightly greater after the short-circuit than before.

The other specimen, subjected to two short-circuit tests, gave similar results. On one phase there was a significant increase in discharge magnitude after each short-circuit test, but in each case this disappeared with heat cycling. The presumption is that the short-circuits caused voids which could give rise to significant discharges, but that these filled up during subsequent heat cycling or rest.

The discharge-inception test is extremely sensitive, and experience in its use in the present connection is small, but the results suggest that the short-circuits imposed caused no significant damage to the cable insulation.

* The magnitude of the discharge given in picocoulombs is the apparent charge lost by the specimen as a result of the discharge.

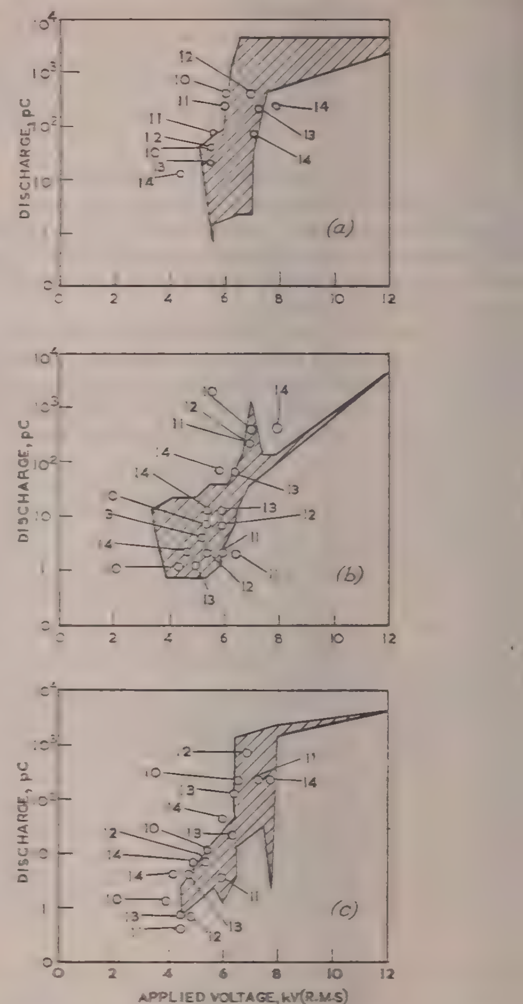

Fig. 18.—Discharge inception voltage characteristics for Specimen 4. Pre-short-circuit results were within the shaded areas.

(*a*) Blue phase. (*b*) Yellow phase. (*c*) Red phase.

10. After short-circuit.
11. Six days after short-circuit.
12. Seven days and 3 heat cycles after short-circuit.
13. Eight days and 6 heat cycles after short-circuit.
14. Eight days and 6 heat cycles after short-circuit (repeat measurement).

(7) MISCELLANEOUS EFFECTS

(7.1) Strength of Soldered Joints

A soldered joint is a potential weakness, since melting of tinman's solder commences at 183°C. The temperature must not approach this figure too closely, since loss of strength in the solder will reduce the strength of the joint in compression. Beavis shows[11] that at 160°C a soldered joint enclosed by a ferrule may be expected to have a tensile strength of 12 800 lb/in² of conductor section, and a plain soldered joint, 11 200 lb/in².

The recommendations of the present paper result in limitation

of compressive forces in the joint to a value less than 8 000 lb/in^2 of conductor area. Since tensile forces will in general be less, there is certainly no danger if the temperature of the joint does not exceed 160° C. The same limit, as Beavis shows, will avoid danger of conductors pulling out of ferrules in those cases where expansion is not restrained but contraction may be, as with cables in air.

The maximum joint temperatures will usually be less than the conductor temperature because the joint efficiency is usually near unity and the thermal capacity per unit length greater for the joint than for the conductor. Beavis suggests that the maximum temperature of a plain soldered joint will be about 20° C less than that of the conductor, and that of a ferrule joint, even neglecting the thermal capacity of the solder, 70–80% of that of the conductor. Numerous observations on 0·15 in^2 cables in the course of the present tests have shown values of 37%–82%, with a mean of 54%. There is thus a considerable margin of safety.

(7.2) Trifurcating Boxes

Trifurcating boxes have not been studied in detail in the present investigations, since they can probably be dimensioned to stand the inherent thermal and mechanical condition, and will often be protected from the full thrust of conductor expansion by bends in the cables approaching them. When the latter is not the case, boxes may fail in two ways. The conductors may buckle, as they do in a joint. Also, the copper rods outgoing through the ceramic insulator are often secured internally by a nut or arch, externally by a nut and lead washer. The internal securing member may yield under thrust from the conductors, and bend or break, allowing emission of cable oil and compound. The channel so formed is likely to contain a soft mixture of oil and compound, pervious to water under gravity. If vertical, such a channel can admit moisture to the insulation.

Both points should be taken account of in the installation of cables for high short-circuit duty.

(7.3) Other Factors

A number of factors other than those discussed above have been considered. None has been found to have a decisive effect except the influence of automatic reclosure on permissible short-circuit ratings. This is of considerable importance. Allowance can be made for it, but it is best treated as a separate subject after the basis of short-circut ratings has been agreed.

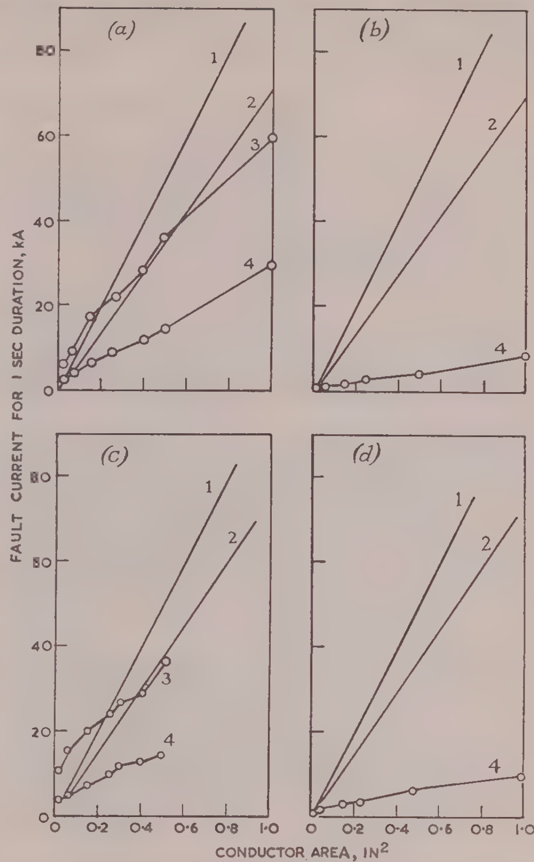

Fig. 19.—Short-circuit limits for various types of cable to B.S. 480: 1954.

(a) 4-core 1 100-volt cables.
(b) Single-core 1 100-volt cables.
(c) 3-core 11 kV cables.
(d) Single-core 11 kV cables.

1. Conductor current limitation due to charring of dielectric.
2. Conductor current limitation due to damage at joints.
3. Total sheath and armour currents, limitation due to sheath temperature, for single-wire armoured cables.
4. Sheath current, limitation due to sheath temperature for unarmoured cables.

(8) REVIEW

Three of the factors examined above may be decisive, namely

(a) Damage to joints due to conductor expansion in multicore cables.
(b) Sheath heating in multicore and single-core cables.
(c) Bursting of sheath and belt in multicore cables.

No factors corresponding to (a) or (c) have been examined for single-core cables. For these, sheath-current limitations are likely always to be decisive in the conditions of use envisaged (maximum earth- and 3-phase-fault current equal in magnitude and duration). Special single-core cables with non-ferrous wire armour may form an exception. The factors corresponding to (c) are determined by details of methods of installation and not by characteristics of the cables themselves.

Fig. 19 shows the respective limits for 1 100-volt 4-core and single-core, and for 11 kV 3-core and single-core cables, expressed as permissible currents for faults of 1 sec duration. Each diagram includes for comparison the line indicating the limit as determined by considerations of charring, and on the diagram for single-core cables, the line relating to joint damage on 3-core cables of the same section. Sheath current limitations are decisive in all cases except those of very small unarmoured multicore cables, single-wire armoured 3-core 11 kV cables up to 0·3 in^2, and single-wire armoured 4-core 1 100-volt cables up to 0·5 in^2.

For fault durations of 0·2 sec or less, the suggested currents to avoid bursting are decisive for 0·3 in^2 and larger 11 kV multicore cables, but are above the rated short-circuit currents of standard circuit-breakers. The corresponding size for 1 100-volt cables is 0·2 in^2. These limits need not be applied when circuits are protected by h.r.c. fuses.

The sheath-current limits defined for wire-armoured cables rely on the probability that sheath and armour will be directly connected at or near the point of a fault to earth, but no allowance is made for the fact that a proportion (perhaps small) of the earth-fault current in a single-core-cable system will be returned by sheaths of the other cables of the circuit if sheaths are bonded.

The conductors and envelope of a cable may each be said to have a definite short-circuit rating, but the cable as a whole has a specific short-circuit rating only in relation to use in a situation in which both 3-phase-fault and earth-fault currents are defined.

The situation envisaged in the present paper is that prospective fault currents of the two kinds are equal in magnitude and duration. Instances in which this does not apply require individual consideration.

(9) CONCLUSIONS

(i) The factors which determine short-circuit ratings of single-core and belted multicore lead-sheathed paper-insulated cables up to 11 kV are

(*a*) Damage to joints by thermal expansion of conductors.
(*b*) Damage to lead sheath by heating.
(*c*) In the case of multicore cables, bursting of cable by electromagnetic forces.

(ii) Each of these has been examined analytically, and average characteristics of standard cables and normal joints have been determined, from which it has been possible to propose limits of operation.

(iii) From (i)(*a*), the conductors of multicore cables should not be allowed to reach a temperature more than 120° C (11 kV cables) or 100° C (1 100-volt cables) higher than a temperature at the mean of the normal operating range at which the cables and the joints may be expected to be free of mechanical stress.

(iv) Lower limits are necessary unless strict attention is paid to the mechanical rigidity of joints, which should be capable of sustaining without damage a longitudinal thrust of 8 000 lb/in^2 of total conductor cross-section.

(v) To permit comparison with previous practice and with practice in other parts of the world, it is desirable to express conclusion (iii) in terms of a permissible conductor temperature. This is 160° C for both 1 100-volt and 11 kV cables.

(vi) Account must be taken of the fact that normal maximum operating temperatures of 11 kV cables are lower than those of 1 100-volt cables. Allowing for this, the permissible short-circuit duty is specified by

$$I = 76{\cdot}5A_c/\sqrt{t} \quad \text{kiloamperes for 11 kV cables} \quad . \quad (12)$$

and $$I = 70A_c/\sqrt{t} \quad \text{kiloamperes for 1 100 V cables} \quad . \quad (13)$$

where t(sec) and I(kA) are the prospective short-circuit duration and current, and A_c(in^2) is the gross cross-sectional area of each conductor.

(vii) The only margin of safety provided by the above recommendation arises from the fact that there will normally be a small heat loss for the conductor during short-circuit, of which no account is taken. It is expected that a few cables operated to this limit, and subjected to the full short-circuit duty when already at normal maximum operating temperature, would suffer some damage at the joints.

(viii) From (iv), lead sheaths should not be allowed to reach a nominal temperature above 250° C, calculated neglecting heat loss during short-circuit. There is a hidden margin of safety in that there will normally be a significant heat loss to insulation, serving or bedding, which will reduce the maximum temperature by at least 10%, and more in the case of large cables. Such a limit will avoid immediate damage to the sheath and induce only trivial changes in grain structure of the sheathing material.

(ix) Wire armouring will afford substantial relief to the sheath. Allowance for this may be made when armour clamps are satisfactory and armour is not subject to corrosion, and should be calculated on the basis of relative d.c. resistances of sheath and armour at 20° C.

(x) From (i)(*c*), conductors should not be subjected to currents in excess of those calculated from formula (7), irrespective of duration. This formula provides a margin of safety of about 2 : 1 in the case of unarmoured cables. The limitation will probably be decisive only over a small range of cables. It need not be applied to cables protected by h.r.c. fuses.

(xi) A cable circuit can be said to have a single short-circuit rating only when incorporated in a system such that prospective 3-phase-fault currents and prospective earth-fault currents are equal in magnitude and duration. In these circumstances, either sheath-current or conductor-current limitation may be decisive, depending on the construction of the cable.

(xii) Attention is drawn to the need for ensuring that trifurcating boxes and similar accessories are capable of withstanding the duty imposed by short-circuit.

(10) ACKNOWLEDGMENTS

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(see next page).

[The discussion on the above paper will be found on page 205.]

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SHORT-CIRCUIT RATINGS FOR MAINS CABLES

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SUMMARY

Research work carried out by the E.R.A. has enabled distribution engineers to assess with greater accuracy the strength of paper- (or cambric-) insulated cables to resist the forces arising from severe short-circuits.

E.R.A. Report Ref. F/T195 describes the research work which has been carried out over a number of years; the present paper interprets the results of the research and provides curves which illustrate the short-circuit currents which can be safely carried by such cables at working voltages up to 11 kV.

(1) INTRODUCTION

The first acceptable recommendations for currents which cables might safely carry under short-circuit conditions were made by Melsom[1] in 1937. His calculations were based on the assumption that cable conductors already running at their then maximum recommended temperature could safely be raised by another 50° C for a short period under conditions of severe short-circuit. This gave a figure of a permissible short-circuit current of 55 kA/in^2 for 1 sec or 125 kA/in^2 for 0·2 sec under the conditions pertaining at that time.

It was to be assumed that no heat was dissipated from the conductor and that the short-circuit current would remain steady during those short times. For very brief short-circuits both these assumptions are reasonably justifiable, but for longer periods of up to 3 or 5 sec there would be some dissipation of heat and also some decrease of the short-circuit current owing to the greatly increased temperature of copper conductors, etc. These factors operated in favour of the cable. The same assumptions are still to be recommended.

The 1937 recommendations were simple and have given reasonably satisfactory guidance to users over the years. They were, however, based on the empirical assumption that a normal paper-insulated (or varnished cambric-insulated) cable might fail in service after it had been subjected, however briefly, to a conductor temperature exceeding 120° C. This has sometimes placed limitations on the sizes of cable to be used in certain installations, and much pressure has been applied to examine the problem more closely so as to determine the real safe limits for short-circuit currents.[2, 3]

This has now been done, and E.R.A. Report Ref. F/T195 deals with short-circuit ratings of cables.[4] The new recommendations are that, in certain cases, the safe conductor temperature may be raised to 160° C. The exceptional cases arise when the return fault currents in the lead sheath raise the sheath temperature to 250° C or when the repulsive forces produced by the fault currents are sufficient to burst the belt insulation and lead sheath of the cable. In all cases the limit is based on mechanical damage such as buckled joints, burst belt insulation or cracked lead sheaths. Charring of insulation or melting of solder in joints does not impose any limit on short-circuit currents more severe than these mechanical considerations.

The new recommendations are more detailed and therefore more complicated than the previous ones. They require more skill on the part of the engineer who uses them. Nevertheless, if Electricity Boards are to make the best use of their restricted resources and plan their system networks economically, they must accept the responsibility for accurate calculations not only of their system short-circuit levels but of the capacity of their cables to withstand them. The paper is intended to give some assistance and guidance in taking advantage of the changes arising from a closer knowledge of what happens under short-circuit conditions based on the E.R.A. research work.

The new recommendations apply to normal belted-type paper-insulated and cambric-insulated cables of up to and including 11 kV working voltage. Although a very great deal of experimental work has been done by the E.R.A. on these types of cable, much more work has still to be done before the results can be applied with safety to, say, screened-type cables for either 11 kV or higher voltages, or to cables with aluminium conductors or solid or compacted copper conductors, or even to installations making use of unusual mechanical joints. It follows that some skill is required if engineers are to avoid the danger of applying the more sophisticated formulae out of the context of their experimental work, and reference must be made to the original work in special cases.

(2) H.V. NETWORKS

Figs. 1–4 illustrate the currents which can be carried safely by various sizes of h.v. cable for periods of up to 3 sec. The currents shown in Fig. 1 will raise the conductor temperature from a stress-free temperature[2] of 40° C to a maximum safe temperature of 160° C. Beyond this temperature it is to be expected that trouble will result from buckled joints.

It is found that good modern joints,[5] which have greater mechanical strength under compression, would withstand buckling better than joints of older design. It follows that they are safer under short-circuit conditions, and curves in Figs. 1–4 are based on the assumption that the cable has been installed in accordance with the best modern practice. In older installations the safe currents should be reduced by 10%. In some high-voltage cable installations there are no joints in the run of the cable. Nevertheless there must be an end dividing box of some sort and it is to be assumed that conductors can be buckled or damaged in such boxes if the temperature rises above 160° C.

Figs. 2, 3 and 4 show the safe currents which may be carried on the assumption that all the fault current will return along the lead sheath and heat it up to a maximum safe temperature of 250° C. Beyond that temperature cracked lead sheaths may be expected.

Figs. 1, 2 and 3 also indicate the limiting conductor current which can be carried in multicore cables before physical bursting of the paper belt and lead sheath becomes possible. With these currents in mind it is necessary to consider the uses to which h.v. cables are put. Examples are taken from the distribution practice of Electricity Boards.

Mr. Buckingham is with the Midlands Electricity Board.

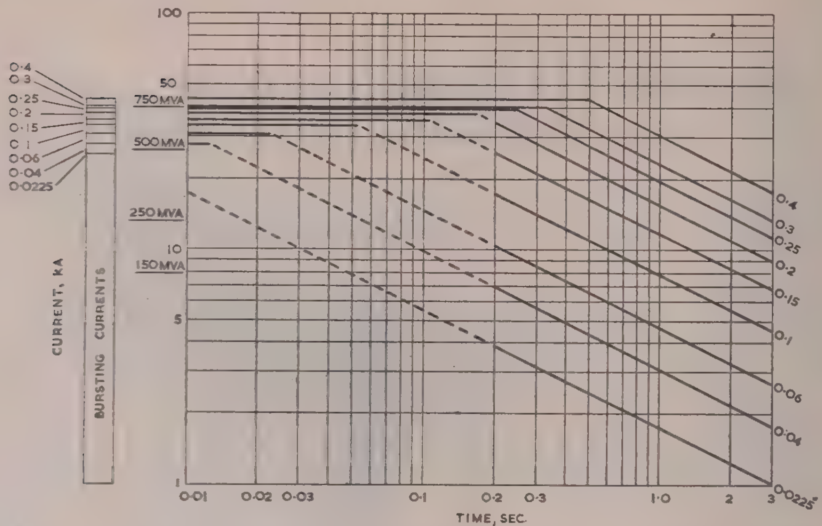

Fig. 1.—Safe cable conductor currents in 11 kV 3-core (belted) single-wire armoured cable to B.S. 480: 1954, based on maximum conductor temperature of 160° C or currents required to burst belt insulation.

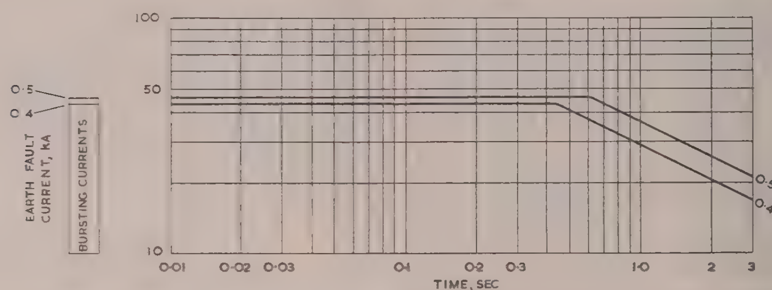

Fig. 2.—Safe cable conductor currents in 11 kV 3-core (belted) single-wire armoured 0·4 and 0·5 in² cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C or currents required to burst belt insulation.

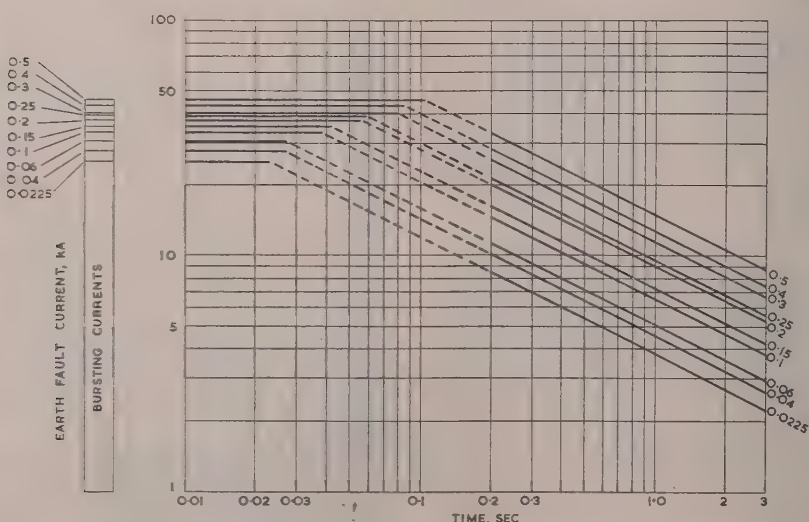

Fig. 3.—Safe cable conductor currents in 11 kV 3-core (belted) unarmoured or steel-tape-armoured cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C or currents required to burst belt insulation.

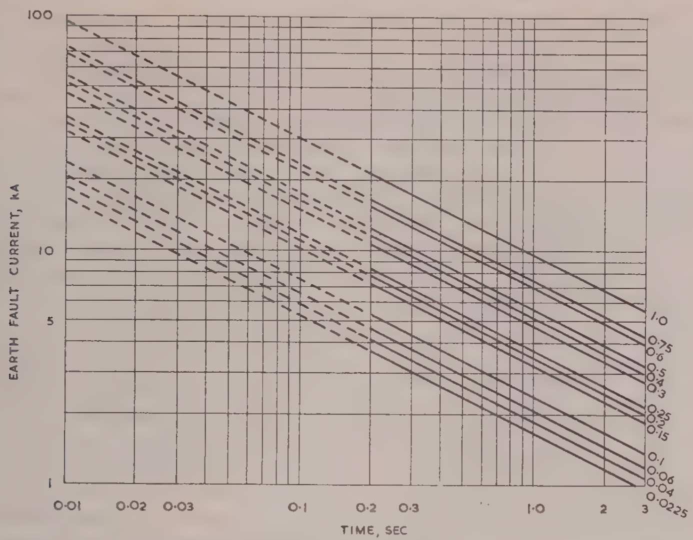

Fig. 4.—Safe cable conductor currents in 11 kV single-core cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C.

(2.1) Grid Substations

If paper-insulated lead-covered cables are used between Grid transformers and the main 11 kV or 6·6 kV switchboard they must be of single-core construction to enable them to carry the heavy load currents concerned. The normal reactance of Grid transformers is specified by the C.E.G.B. as 20%, so that it is usually possible to use switchgear having a rating of 250 MVA at 11 kV. Since single-core cables are used there is little likelihood of a phase-to-phase fault developing along the cable runs. In the event of such a short-circuit occurring in any other part of the system, the Tables show that it would not damage these very large cables within 3 sec. During that time it is to be expected that the fault will be cleared by the switchgear protecting the system.

An earth fault to the lead sheath of one of these single-core cables would, however, cause enough current to flow to damage the lead sheaths, if it were not limited by a neutral earthing resistance or reactor. It is normal practice to earth the neutral points of such systems through a resistance designed to keep the earth fault current to 2 kA or less. It follows that no special precautions are necessary for these large single-core cables by reason of short-circuit currents likely to pass through them.

Feeder cables from the 11 or 6·6 kV switchboard of a Grid substation would normally be of 3-core construction.

The situation in respect of 3-core high-voltage cables is not bad if such cables are single-wire armoured. The armouring relieves the lead sheath of a share of the return earth-fault current and conductor temperature becomes the limiting factor. (To do this the armour bonding must be in good condition and the armour wires themselves must not have been corroded away. This is an important qualification if cables are laid in bad soil. If the conductivity of the armouring has been reduced by bad bonding or corrosion, the cable should be regarded as plain lead covered and the short-circuit rating reduced accordingly.)

Fault currents fall away reasonably rapidly with distance, so that, even if a larger cable is needed near the substation, a reduction in size might be possible at the first joint position. For example, a 3-core $0 \cdot 3\,in^2$ p.l.y.s.w.s. cable is quite safe with a fault level of 250 MVA for 3 sec. Within 500 yd the impedance of the cable itself has reduced the possible fault level to 217 MVA, making a 3-core $0 \cdot 25\,in^2$ cable safe. An earthing resistance will not, of course, prevent a full-scale short-circuit between phases, and 3-core cables must therefore have conductors large enough to deal with the full fault level of the system at that point.

If the 3-core cable is unarmoured or is steel-tape armoured, or the wire armouring is not in good condition, the sheath temperature under double-earth-fault conditions becomes the limiting factor again and even a 3-core $0 \cdot 50\,in^2$ p.l.y. cable is safe only for 1·3 sec. The safe fault current for, say, a 3-core $0 \cdot 3\,in^2$ p.l.y. cable for 3 sec is 6·4 kA (equivalent to a symmetrical 3-phase fault of only 122 MVA). The recommendation must therefore be that single-wire armoured cables should always be used where the short-circuit energy level is high. At 6·6 kV with the same short-circuit fault energy the need for special precautions becomes even more obvious, and the safe symmetrical fault level for a 3-core $0 \cdot 30\,in^2$ p.l.y. cable falls to 73 MVA.

The failure of the lead sheath under high temperatures owing to fault currents is due to the mechanical forces arising from the unequal expansion and contraction of copper conductors and lead sheaths. The calculations in Reference 4 are based on an average sheath temperature of 60° C. It does not follow that a cold plain lead-covered cable laid in the ground at 15° C has a larger factor of safety under short-circuit conditions.

Although the formulae produced as a result of the E.R.A. researches can theoretically be extended from zero to any length of time for a short-circuit, it is reasonable to apply certain practical limitations. For h.v. cables it has been usual to ignore any values below 0·20 sec or above 3 sec. While it is true that high-speed methods of protection are able to disconnect a faulty cable in less than 0·20 sec, it is undesirable, in practice, to adopt the very high readings of fault current which would result from the use of such short times.

It is better to assume, for this purpose, that high-speed gear might not operate at the crucial moment and to provide a cable which will be safe until the back-up protection comes into operation and clears the fault. It is normal for the C.E.G.B. to require Area Boards to disconnect faulty networks within 3 sec, and this is regarded as a reasonable upper limit when using short-circuit rating curves.

The curves reproduced indicate the safe ratings of cables when

carrying fault currents for periods of up to 3 sec. The curves show in some cases the equivalent short-circuit rating in MVA at 11 kV on the assumption that the fault is a 3-phase symmetrical short-circuit, which is the normal yardstick of the distribution engineer. The formulae by Gosland and Parr on which these curves are based take into account the fact that asymmetrical fault currents occur in practice. Those authors did not deal with the question of reclosing switchgear on a fault. It is clear from the curves that repeated earth-fault currents of only 1·5 kA are unlikely to damage h.v. cables, but severe phase-to-phase faults might well do so. Instructions to operating engineers should take these considerations into account.

(2.2) Distribution Substations

If supplies are distributed from Grid substations at 33 kV to distribution centres for transformation to 11 kV, the same remarks apply as in Section 2.1. It is normal to use earthing resistors or reactances to limit the earth-fault current for the protection of single-core transformer connections, and such cables are then safe from short-circuit forces likely to be encountered at such points. If it is assumed that the fault level on the 11 kV distribution network is limited by the natural (or designed) reactance of the system to 150 MVA, it can be said that a 3-core 0·20 in^2 p.l.y.s.w.s. 11 kV feeder cable is safe for 3 sec, and a 3-core 0·10 in^2 similar cable would be safe for 1 sec. At 6·6 kV correspondingly larger cables become necessary. Many miles of much smaller cables than these have, of course, been installed under such prospective fault conditions, and the amount of trouble that has been experienced has been negligible. This is, no doubt, due to the fact that most faults start as earth faults which are limited by neutral earthing resistances and therefore do not damage the cable, and also to the correct high speed of operation of relays and tripping of switchgear.

Distribution engineers have some new problems to face in the future as loads get larger. It is already difficult in heavily loaded areas to limit the short-circuit level on the 11 kV system to 150 MVA. Switchgear makers nearly all guarantee their 11 kV normal distribution and industrial-type switchgear for 250 MVA, and it will be a great advantage in the future if the short-circuit level of 11 kV networks can be raised to 250 MVA. But will the distribution cables be safe?

It has already been shown that, for a short-circuit duty of 250 MVA it required a 3-core 0·3 in^2 p.l.y.s.w.s. cable to be safe for 3 sec; a 3-core 0·20 in^2 cable will heat up in 1·4 sec and a 3-core 0·10 in^2 cable in only 0·3 sec. Distribution engineers plan their systems for many years ahead with h.v. reinforcement schemes in mind, and they must be prepared to lay cables large enough for the prospective short-circuit duty which will arise.

It is already probable that the thickness of insulation of 11 kV cables is held at its present figure more by reason of the need to withstand switching and atmospheric surges than the need to withstand the working voltage. It is equally probable that, in many cases, the conductor size of such cables should be fixed by having regard more to the short-circuit capacity than the load-carrying ability. If so, it is an important point to remember.

When a cable has a conductor larger than is necessary to carry the load it will run cooler than the maximum design temperature. This, in turn, lowers the 'stress-free' temperature* referred[4] to in E.R.A. Report Ref. F/T195, and thus increases the temperature rise available before a conductor temperature of 160° C is reached. It is not recommended that this extra temperature rise which becomes available should be used in design calculations, because the danger of buckled joints arises from the expansion of conductors based on a temperature rise of 120° C. (This is to be compared with the fact that in calculating current-carrying capacities of underground cables it is not considered good practice to assume that the temperature of the earth ever falls below 15° C, but fortunately at times of the heaviest load in the winter the soil temperature is often lower and a factor of safety becomes available.)

It is necessary to consider the economics of designing against short-circuit conditions, especially if it appears necessary to use a larger conductor than is required for load carrying. Since the distribution network has its neutral earthed through a current-limiting system, the use of single-core cables to avoid phase-to-phase faults may be economic. Or, perhaps, the use of screened-type cables should be encouraged—provided that the screen contains sufficient metal to guarantee safety against phase-to-phase faults, and that the screen is continued through joints and terminations. The E.R.A. Report does not deal with screened cables, but the point is made that, if phase-to-phase faults can be avoided in 3-core armoured cables, the fault duty on them can be much reduced to the earth-fault conditions controlled by neutral earthing resistances.

There will always remain the risk of double-earth faults and, of course, phase-to-phase faults in other equipment such as switchgear busbars, transformers, etc. These risks must be assessed when deciding on the merits and advantages of phase segregation of cable conductors.

E.R.A. Report Ref. F/T195 has emphasized the need to consider short-circuit conditions in planning the use of cables. But it should be remembered that the new recommendations are in most ways less onerous than the old ones which should have been applied since 1937.

(3) L.V. NETWORKS

Figs. 5–9 illustrate the currents which can be safely carried by various sizes of l.v. cable for periods of up to 3 sec. It quickly becomes clear that l.v. cables cannot be protected by devices which take 3 sec to operate and that back-up protection like h.r.c. fuses is essential.

Fig. 10 illustrates a typical family of short-circuit characteristic curves of h.r.c. fuses. These must be read in conjunction with the cable characteristic curves, and generally it will be seen that, by proper selection of fuse sizes, l.v. cable short-circuit currents will be interrupted before any damage is done to the cable.

(3.1) Distribution Substation

Single-core l.v. cables which run from the secondary side of a distribution transformer cannot normally withstand with safety the earth fault currents likely to occur in them. Even a 100 kVA transformer whose normal full-load current is only 133 amp at 433 volts between phases should have 0·60 in^2 single-core cables from its l.v. terminals for them to be safe—for 3 sec. These cable runs from a substation transformer to the first distribution fuseboard are usually so short that the risk of damage can be taken without much fear. It does, however, exist and it is sensible to take as much care as possible to see that they are installed without being physically damaged.

It is probable that most distribution engineers are aware of the short-circuit possibilities on the 4-core or 5-core distribution cables laid from their substations. It is not permissible to limit the earth fault currents with the aid of neutral earthing resistances as in the case of h.v. cables.

The short-circuit currents are limited only by the natural impedances of the h.v. system and the substation transformer

* The temperature at which a cable conductor is neither under tension due to cooling nor compression due to heating is termed the 'stress free' temperature. Gosland and Parr have shown that this temperature is half-way between the lowest temperature of an unloaded cable and the maximum operating conductor temperature.

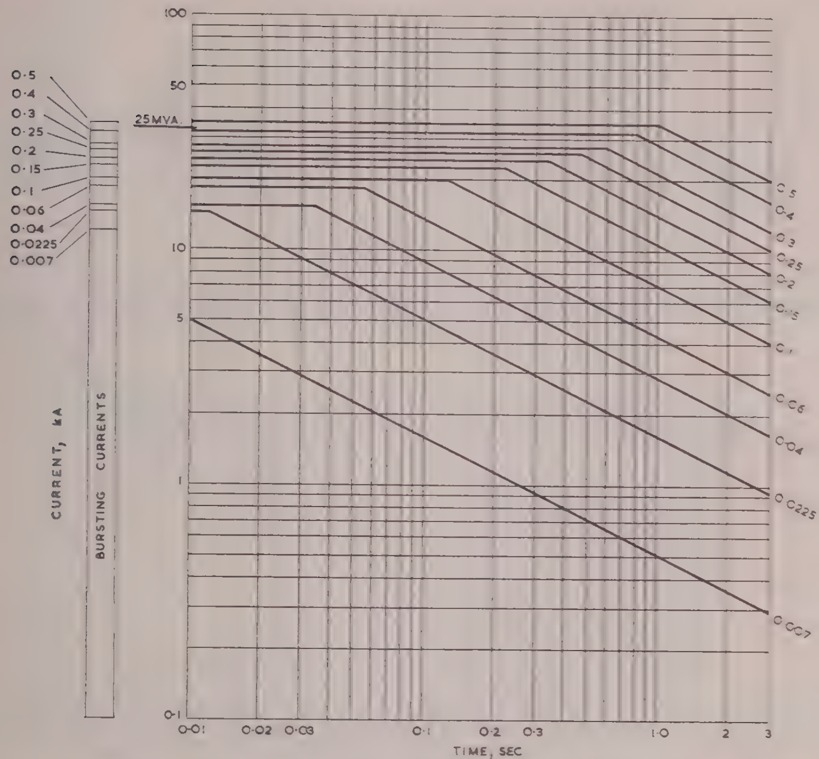

Fig. 5.—Safe cable conductor currents in 1·1 kV 4-core (belted) single-wire armoured cable to B.S. 480: 1954, based on maximum conductor temperature of 160° C or currents required to burst belt insulation.

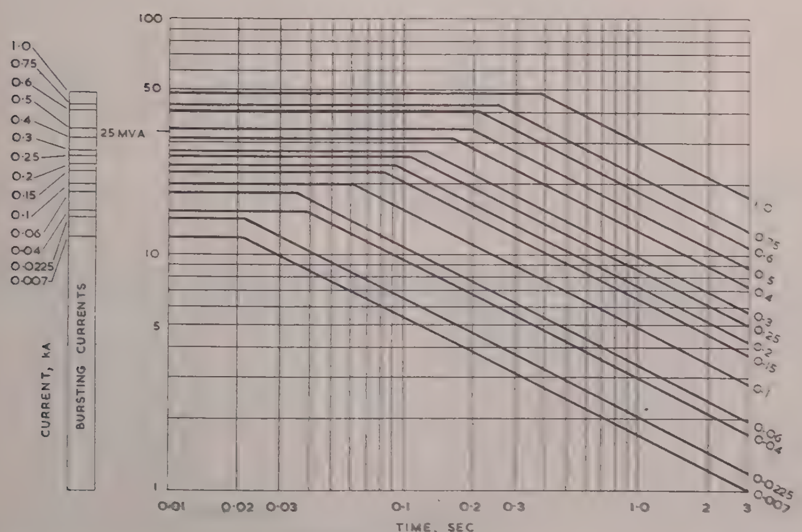

Fig. 6.—Safe cable conductor currents in 1·1 kV 4-core unarmoured or steel-tape-armoured cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C or currents required to burst belt insulation.

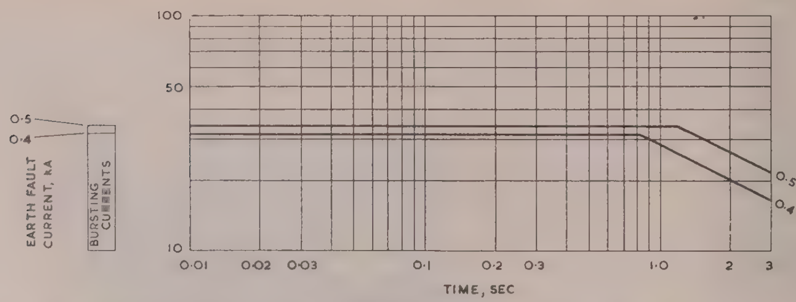

Fig. 7.—Safe cable conductor currents in 1·1 kV 4-core (belted) single-wire armoured cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C.

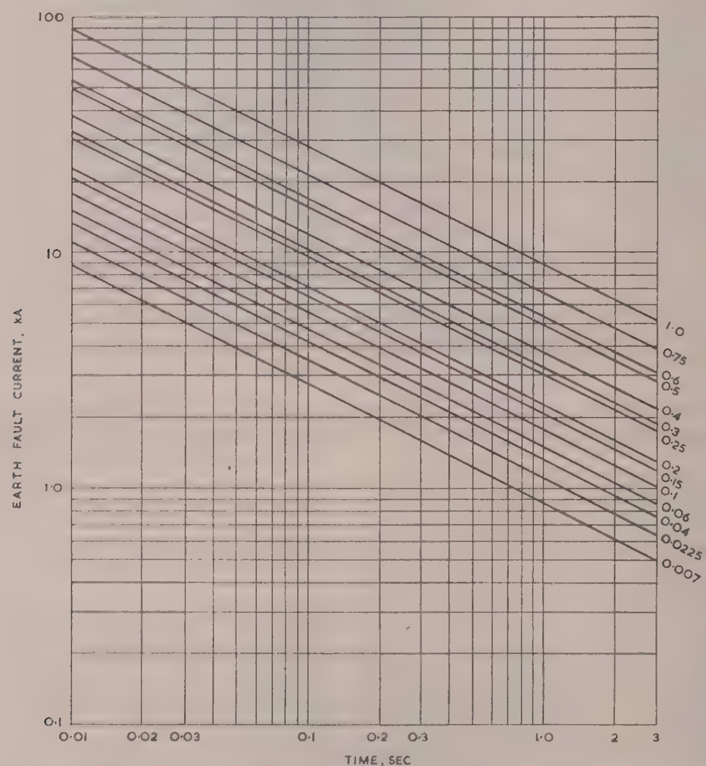

Fig. 8.—Safe cable conductor currents in 1·1 kV single-core cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C.

concerned. A modern distribution transformer substation can easily provide a short-circuit level of 25 MVA at 433 volts (i.e. 33 kA). A 4-core $0\cdot3\,in^2$ p.l.y.s.t.s. cable would only be safe at 25 MVA for 0·10 sec, and a 4-core $0\cdot06\,in^2$ cable could not be protected. Although in the case of h.v. cables it is usual to consider the possibility of a short-circuit lasting 3 sec, with l.v. cables it is impossible to do so. Such cables must be protected by h.r.c. fuses, which act so quickly that they interrupt the circuit before the full prospective short-circuit current has time to develop. If rewireable fuses which act more slowly are used, it is desirable to compare the operating time curves with the cable short-circuit curves to ensure that they disconnect the supply before the cables are endangered. The fact that l.v. cables are seldom run at their maximum safe temperature means that their stress-free temperature is low, but this does not necessarily provide a margin of safety to help such cables to withstand the effects of short circuits.

Gosland and Parr[8] stress the importance of well-designed, mechanically robust and well-made cable joints on the l.v. distribution system.[6] This has perhaps been neglected in the past, but the Electricity Boards have now published a code of practice for l.v. joints and those designs do comply with modern requirements.

In London, and also in New York and Berlin, large sections of the l.v. network are solidly connected together with time-delayed l.v. protection and fed from a large number of medium-size transformers in different parts of the system. If a fault develops on the l.v. system energy is fed into the fault until it burns itself clear or the time-delayed protection operates.

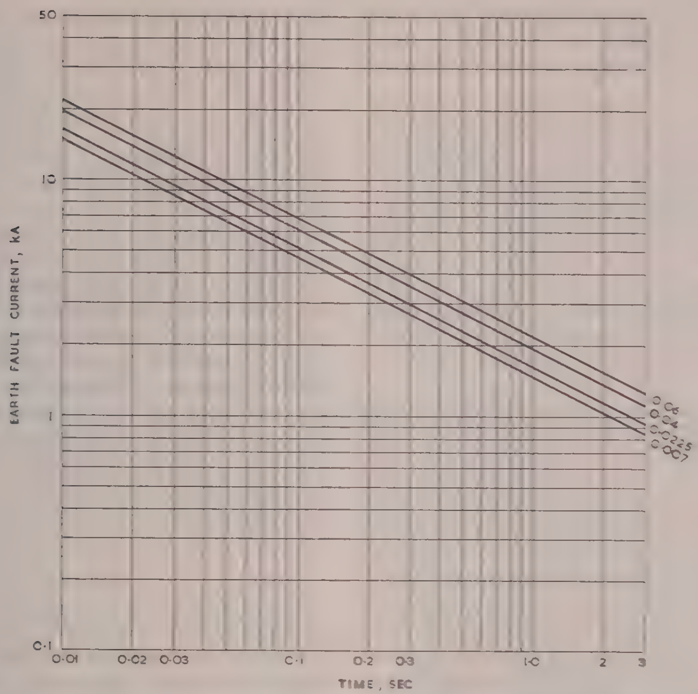

Fig. 9.—Safe cable conductor currents in 1·1 kV twin (belted) unarmoured or steel-tape armoured cable to B.S. 480: 1954, based on maximum sheath temperature of 250° C.

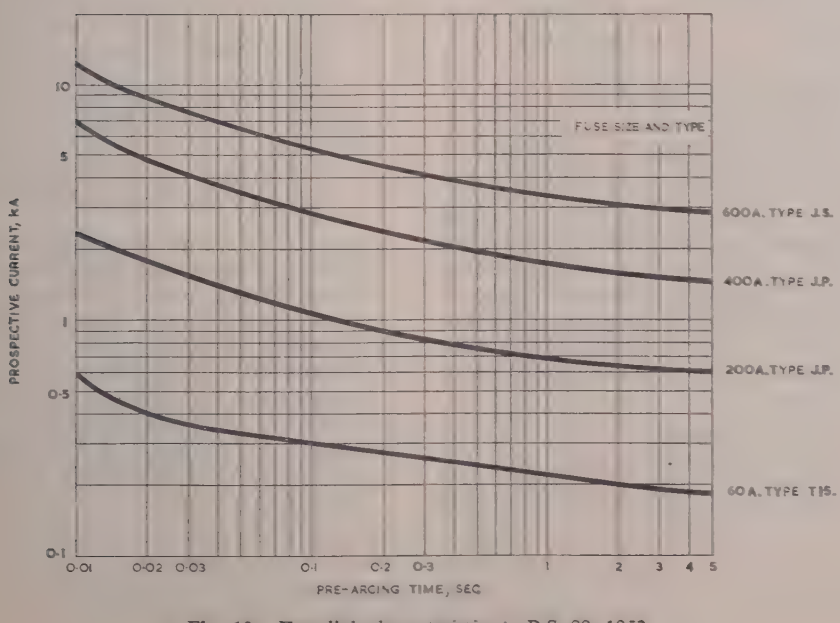

Fig. 10.—Fuse-link characteristics to B.S. 88: 1952.

Generally these will be in the form of high-resistance earth faults and the prescribed temperature limits will not be exceeded. Sometimes, however, more onerous short-circuits occur and some damage is done. It is obvious that where they are used the advantages of the solid networks outweigh the disadvantage of these infrequent occurrences.

Perhaps the greatest risks in electricity supply are taken with underground service cables. A twin $0 \cdot 0225\,in^2$ p.l.y.s.t.s. cable connected solidly in an underground tee joint to a 4-core $0 \cdot 30\,in^2$ main near a substation having a short-circuit level of 25 MVA would be fully protected by a 600 amp fuse, but at a distance of about 500 yd from the substation where the short-circuit level was, say, only 3 MVA, a 600 amp fuse would take such a long time to blow that any conductor smaller than $0 \cdot 06\,in^2$ would be damaged. The fuse rating should therefore be matched to the known load conditions of the distributor cable so that smaller cables connected to it can be protected, care being taken to provide for normal load growth.

It is fortunate that the robust construction of such installations reduces to a minimum the possibility of a short-circuit occurring. Even twin $0 \cdot 007\,in^2$ street-lighting cables have given very little trouble. Short-circuits which occur in domestic premises or in street-lighting columns are usually protected by small h.r.c. fuses and do not overload service cables or mains cables. It would not really be sensible to increase the size of service cables to domestic premises in order to safeguard them against short-circuits, because this would only tend to raise the short-circuit level at the householder's service position. Although it is usual for Electricity Boards to use h.r.c. fuses of 25 MVA rating at service positions, the consumer's fuseboard is frequently equipped with rewireable fuses or with miniature circuit-breakers of much lower breaking capacity. A few yards of twin $0 \cdot 0225\,in^2$ service cable may help to reduce the short-circuit level of the supply to within these limits.

The fifth core of a 5-core distributor cable is usually protected by a 60 amp h.r.c. fuse so that it may be regarded as being safe. A length of such conductor again reduces the short-circuit level at street-light positions and adds to their safety.

The new recommendations are not likely to affect the sizes of cables used for l.v. distribution mains. They have, however, enabled distribution engineers to make a more accurate assessment of the dangers arising from the use of small cables.

(4) INDUSTRIAL INSTALLATIONS

(4.1) Works Substation

In large industrial installations the form of distribution is often very similar to an electricity-supply distribution network. In such cases the Electricity Boards' practices should be followed, i.e. l.v. cables should be protected by h.r.c. fuses. In many cases, however, works engineers have protected their mains cable runs with l.v. automatic circuit-breakers, and even though these are fast in operation, it is possible for maloperation or delay to endanger quite long lengths of cable in the event of a short-circuit. An h.r.c. fuseboard as close as possible to the works transformer would provide high-speed back-up protection to ensure the safety of the cables.

(4.2) Power Stations

The main switchboards controlling the auxiliary circuits[7] of power stations have increased in short-circuit rating from 150 MVA at $3 \cdot 3$ kV to 250 MVA at $6 \cdot 6$ kV, and in future will rise to 500 MVA at 11 kV.,. The neutrals of the unit transformers and station transformers are earthed through resistances, so that the only danger to cables connected to these boards is from phase-to-phase faults. In these circumstances it has become the custom to use a minimum size of 3-core $0 \cdot 20\,in^2$ conductor to a 150 MVA $3 \cdot 3$ kV switchboard, even though the load to be carried may be only 10 amp. Such a cable would withstand the full possible rated short-circuit current for $0 \cdot 30$ sec before overheating. Corresponding sizes are used on the higher voltages. It is important when such large cables are used for such small loads that cable terminating boxes on equipment are designed to be large enough for the short-circuit condition and not the load conditions, otherwise the risk of breakdown in cable end boxes is much increased.

Main generator connections are usually of bare copper, but tee-off connections to unit transformers may be of single-core cable. These are so large and the neutral earthing arrangements so closely controlled that little danger arises in the most unlikely event of a short-circuit so close to the generator.

The l.v. switchboards usually have a rating of 25 MVA, and it is recommended that h.r.c. fuses should be installed as back-up protection for oil circuit-breakers or air-break switchgear controlling the many smaller auxiliary supplies.

(5) CONCLUSIONS

The Figures which illustrate the paper take the place of the graph which was published by Melsom[1] in 1937. The information now given is much more detailed than was available 23 years ago and will enable distribution engineers to make the best use of material available to them. The design of power distribution networks is still more of an art than a science, but the introduction of more and more reliable information upon which calculations can be made will result in better and more efficient results.

(6) REFERENCES

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(3) LYTHGOE, W. H., and REYNOLDS, S.: 'Short Circuit Conditions in Cables', *Distribution of Electricity*, January, 1940, p. 340.

(4) 'A Basis for Short Circuit Ratings for Paper Insulated Lead Covered Cables up to 11 kV' (E.R.A. Report Ref. F/T195; 1960).

(5) Code of Practice on the Design of H.V. Cable Joints. Engineering Recommendation C.34 Electricity Council, June, 1959.

(6) Code of Practice on the Design of Cable Joints (Straight Through, Service and Branch Joints for Paper-Insulated Lead-Covered Cables operating at Voltages up to 650 volts). Central Electricity Authority and Area Boards, November, 1954.

(7) DEWISON, D. A.: 'Electrical Supplies to Power Station Auxiliaries', *Proceedings I.E.E.*, Paper No. 2759 S, December, 1958 (**106** A, p. 451).

(8) GOSLAND L., and PARR, R. G.: 'A Basis for Short-Circuit Ratings for Paper-Insulated Cables up to 11kV' (see page 183).

DISCUSSION ON THE ABOVE TWO PAPERS

Before the SUPPLY SECTION 14*th December*, *the* SOUTH MIDLAND SUPPLY AND UTILIZATION GROUP *at* BIRMINGHAM 10*th October*, *the* SOUTH-WEST SCOTLAND SUB-CENTRE *at* GLASGOW 19*th October*, 1960, *the* NORTH-WESTERN SUPPLY GROUP *at* MANCHESTER 31*st January*, *the* WESTERN SUPPLY GROUP *at* CARDIFF 20*th February*, *the* WESTERN CENTRE *at* GLOUCESTER 6*th March*, *and the* NORTH-EASTERN CENTRE *at* NEWCASTLE UPON TYNE 13*th March*, 1961.

Mr. L. H. Welch: The film shown will convince all engineers that it is most undesirable to have a current of 50 kA flowing in a circuit. The Chief Engineers' Conference in 1954 recommended that m.v. and h.v. cables for distribution networks should be armoured. The tests in the papers all concern lead-sheathed cables, and formulae are given to adapt these results to armoured cable. The graphs all concern armoured cable, and I hope that the deductions made to produce them are valid.

I agree with the statement in the paper by Messrs. Gosland and Parr that tape armour is virtually worthless, but a good case is made, from an economic point of view, for using armoured cables. I am interested in the comment that there was no difficulty with malleable iron clamps when carrying the test fault currents, because in all the experiments which the London Electricity Board have carried out the currents were no greater than 12 kA. It was always possible to see that the clamps had been carrying a large current, although sometimes they were reasonably satisfactory.

Mr. Buckingham makes so many pertinent remarks that I think his paper should be given to every distribution engineer to read, digest and follow. I agree with all he says, although I think he is a little pessimistic about m.v. cables and their short-circuit capacity. Experience would indicate that the short-circuit capacities are better, but perhaps this is due to networks designed for rather lower rupturing capacities.

Recently h.v. joints have been standardized, but I think they have a less onerous duty than m.v. joints, and I should like to see more information given concerning m.v. joints and some standardization to be made. There is a tendency to think that m.v. jointing requires a less skilled man than h.v. jointing. This is very doubtful, and in fact the m.v. joint in many ways requires more care than the ordinary 11 kV joint.

Most faults due to short-circuit current occur in the boxes and not on the cables themselves. The author emphasizes that m.v. distribution systems must be supplied by a distributed transformer system; these transformers must not be too large and concentration of power at one point must be avoided if the short-circuit capacity is to be kept to a reasonable figure.

The time suggested for a fault of 3 sec is an onerous duty for any ordinary distribution system, and on the 11 kV network it is probable that it occurs only with busbar faults, if at all. It is debatable whether systems should be designed to cater for this emergency condition.

The papers are of great value to the supply industry, and the work of the E.R.A., under the guiding hand of Mr. Buckingham, is greatly appreciated, but I would make a final plea for further tests on armoured cables.

Mr. P. M. Hollingsworth: I should like to comment on the influence of the lead sheath on limitation of short-circuit ratings for unarmoured cables or cables where the wire armour is suspect. Mr. Buckingham has painted rather a gloomy picture of the predicament of those users who have unarmoured lead-sheathed cable. I do not quarrel in the least with his conclusions, and, in fact, if lead-sheath thicknesses are going to be further reduced (as they may well be in the next British Standard, to line up with Continental Standards) the situation will become even worse. The condition is particularly onerous, as Mr. Buckingham points out, for single-core 11 kV cables, and presumably for such cables a time limit of up to 3 sec will normally obtain.

Associated with all this there is the need to study the economics of short-circuit ratings, and this raises questions about other forms of construction. The aluminium sheath is an obvious alternative. I know that Mr. Buckingham is a user of aluminium-sheath cables (and I hope a satisfied user) in h.v. cable applications, and perhaps that solution may appeal to him for lower voltages.

For example, the corrugated aluminium sheath, which is the most recent development in this field and which provides a thinner and more flexible sheath, has a short-circuit rating, depending on diameter, of anything from $1\frac{3}{4}$ to $2\frac{1}{2}$ times that of an equivalent lead sheath based on 250° C. The strength of aluminium will obviously permit a higher temperature, but it is necessary to consider the temperature effects on the protective covering, probably p.v.c., and it would no doubt be unwise to go higher than 250° C.

The E.R.A. have already done some work on smooth aluminium-sheathed cables. It seems that tests on the thinner corrugated form are now required, with particular reference to the effect of short-circuit on the protective coverings.

The foregoing considerations also apply to l.v. cables—perhaps even more so, because these do not have the benefit of a neutral resistance, though clearances will be very much more rapid. With l.v. cables there may also be the possibility in the future of using a copper sheath, which will probably be thinner and therefore have much the same rating as the corrugated aluminium sheath.

Mr. Buckingham states in Section 1 of his paper that the responsibility for accurate calculations not only of their system short-circuit levels but of the capacity of their cables to withstand them must rest upon the Electricity Board engineers. I would conclude by saying that the cable makers are ready to co-operate by exploiting to the full the recent developments in cable design and construction in order to achieve the most economic solution to any particular problem.

Mr. R. S. Orchard: Until the paper by Messrs. Gosland and Parr gave the very complete information covering paper-insulated cables for 11 kV and below, it had been the practice when assessing the short-circuit rating of higher-voltage cables to work on the basis of 120° C maximum copper temperature. Engineers dealing with transmission systems now have to decide whether they can adopt the new figures of 160° C and 250° C for solid paper-insulated cables above 11 kV and also for pressure cables. It would be useful, therefore, if the authors could express some opinion on whether this is likely to be a satisfactory approach. It is also possible that, owing to the more robust nature of the higher-voltage cables, these temperatures could be conservative.

If short-circuit rating tables were produced for these higher-voltage cables, would it also be practicable, in view of the higher time-constant of the cables, to take into account the effect of cooling during short-circuit? Could account also be taken of the cooling effect of the oil and oil ducts in oil-pressure cables and of the effect of the reinforcement in gas pressure cables?

It has taken a considerable time for the investigations to be completed on 11 kV paper-insulated cables, and there appears to be a huge programme laid down for short-circuit testing of other types of l.v. cables. Can the authors say whether there are any proposals for carrying out short-circuit tests on the higher-voltage cables also?

While Mr. Buckingham's paper gives valuable guidance on the action to be taken in assessing short-circuit ratings of cables for specific installations, the author appears to use a wide short-circuit time range, i.e. 0·2–3 sec, and gives the impression that the times have been settled more on what the cables will stand than on what the system will impose upon them. For distribution-cable systems with time-delayed protection, separate assessment of short-circuit time may well be necessary for each length of cable; but for power-station and transmission circuits where reliable high-speed protection is installed, do the authors agree that, in settling the short-circuit time, reliance should be placed on the operation of the first-line protection. Do they agree that a range of minimum standard short-circuit times could be developed from different types of circuits, while giving a reasonable factor of safety, e.g. from 1 sec for those circuits equipped with a high-speed type of relay protection such as are used for most transmission circuits down to 0·2 sec for the h.r.c. fuse protected circuits used widely in power stations?

Mr. W. H. Lythgoe: When the technical committees of the C.M.A. were formed in the early 1930s it very quickly became apparent that there was a great deal of interest in the short-circuit capacities of cables, and figures were prepared based on a temperature rise of 50° C to a maximum of 120° C under short-circuit conditions. Those figures were deliberately restricted in their application to a time of 0·2–1 sec. Our courage did not allow us to go much further than that, but it seemed that these limits of temperature and time would deal adequately with the known requirements at that date. This procedure gave a very simple, straightforward result; for 0·2 sec the short-circuit rating was 125 kA/in^2, for 0·5 sec it was 80 kA/in^2, and for 1 sec it was 55 kA/in^2.

On the very day that these figures were decided by the C.M.A. technical committee, a paper was presented to The Institution by Clothier, Leeson and Leyburn,* which gave a curve of proposed short-circuit capacities for cables. Not being prepared by cable engineers, it was over-optimistic. A further paper at about the same time by Ward made other and similarly optimistic recommendations with regard to the rating of cables under short-circuit conditions and gave added variety to this untidy position.

When these ratings were first proposed it was customary for paper-insulated cables to be rated at a maximum conductor temperature of 65° C. This, with certain limitations in respect of particular types and voltages, has now been increased to 80° C. Furthermore, in this period, because of changes to British Standards for impregnated cables, the cables now used are less robust than they were when these figures were first formulated.

The C.M.A. felt that it had not gone far enough in proposing a maximum short-circuit temperature of 120° C, and consequently suggestions were made to the E.R.A. that research should be undertaken. As we all know, this has resolved itself into a series of five very valuable papers on various aspects of the subject. It is not surprising, therefore, that under present conditions supply engineers and cable makers are faced with a very much more complex picture in order to assess the rating which can be safely ascribed to a cable under short-circuit conditions. Meanwhile the time period has gone up, and 3 sec is a very reasonable period under the conditions of modern distribution systems.

Mr. C. C. Barnes: In 1937 the late S. W. Melsom recommended an arbitrary basis of 120° C maximum conductor temperature during short-circuit conditions. This is a very conservative level and has been contested for many years by cable users. For certain cables a temperature of 200° C has now been accepted in Germany, Sweden and the United States.

If Mr. Buckingham's paper is strictly followed, larger conductor sizes than those hitherto used will be necessary in some cases. Is the author now working to or planning to follow strictly his proposals for all future cable installations by the Midlands Electricity Board? If so, this will result in increased expenditure on cables.

Successful service experience is an important criterion. I have found it very difficult to get information on cable failures clearly attributable to short-circuit conditions, apart from one or two isolated cases in which some of the opinions expressed are open to criticism or doubt.

When several factors are involved, engineering judgment and experience must be applied. If a short-circuit is maintained for several minutes, cables must be damaged, but when the time is short, i.e. a few cycles or a second or two, the amount of heat generated is only momentarily high and assumed to be confined to the conductor, so that a safety margin is introduced. Furthermore, unless the short-circuit occurs when the cable has been fully loaded for some hours, which is a rare occurrence, a further safety margin is introduced.

Reference is made to loose sheaths and deformation of the lead sheath. Manufacturing practice has varied, but tight lead sheaths can be applied by experienced manufacturers. However, greater use is now being made of aluminium sheaths, and this practice should help if and when short-circuits occur.

I find Section 5.10 and Tables 6 and 7 of the paper by Messrs. Gosland and Parr difficult to understand. From what test data is the stress-free temperature of 40° C deduced? Table 8 gives 10° C twice and 12° C in the third example. For wire-armoured cables wholly in air, secured at intervals of about 3 ft, could a figure of, say, 200° C above the stress-free temperature be used, and what stress-free temperature is permissible for this method of installation?

The coefficient of thermal expansion of aluminium is about 40% greater than that of copper. Does this mean that lower maximum temperature limits must be used for aluminium conductors than for copper conductors? Furthermore, will the greater stiffness of an aluminium sheath decrease the temperature limits recommended by the authors in Section 5.10?

The work of the E.R.A. on the short-circuit rating of power cables has been in progress since 1938. It is unfortunate that the papers are confined to a very limited range of cables. I hope that in the next few years the authors will provide information relating to 33 kV solid-type cables, thermoplastic-insulated cables and pressure-assisted cables for all voltages.

Mr. D. E. Bird: The papers show very clearly that the use of plain lead-covered cable should, in general, be avoided, because the cost of wire armour is certainly less than the cost of additional copper. So far as single-core cables are concerned, however, the curves are frightening, because we cannot armour these cables and the short-circuit capacity appears to be very low. However, the result may not be so bad as it might at first appear, since we never use one cable on its own but always have a group of three. If the earth fault is outside the cable run the fault current, with double bonding, will be shared by three or possibly more cables, and, as the cables are usually fairly short, it is possible to run an earth bond with the cables so that their sheaths do not have to carry all the current. If we are considering a fault on one of the single-core cables itself, that cable will have been damaged in any event. If we have to replace the whole of it because of damage to the lead that is unfortunate, but far cheaper than the alternative.

In Section 2.2 of the paper by Mr. Buckingham there is reference to the use of earth-fault limiting devices, and it is

* Clothier, H. W., Leeson, B. H., and Leyburn, H.: 'Safeguards Against Interruptions of Supply', *Journal I.E.E.*, 1938, 82, p. 445.

stated that the use of single-core cables to avoid phase-to-phase faults may be economic. The author adds, 'This would not comply with the present statutory requirements'. I agree that this statement applies to l.v. systems, but I do not see how it applies to h.v. systems under which heading it appears.

With regard to the bursting strength of 3-core cables, the figures shown in the curves seem, from experience of two cables which burst, to be very high. In one case I know, a $0\cdot1\,\text{in}^2$ 11 kV cable burst at 20 kA, whereas from the paper the figure should be in excess of 30 kA. This particular cable had been buried in the ground for some time, so that the ground had solidified. The cable burst at regular intervals of something like 7 ft. Can the authors provide any explanation for this?

Another point which must be considered, particularly with cables installed fairly near a generating station, is plant decrement. This does not affect the initial short-circuit current, and therefore does not affect the bursting, but it may affect the heating very considerably. To make matters worse there is the question of reclosing, which may be high-speed or low-speed auto-reclosing, or even manual reclosing. Can the authors give us some indication of the sort of derating factors to use after different intervals of time?

Mr. D. T. Hollingsworth: These tests have been carried out on ordinary cables which have been designed from the point of view of carrying current most economically and not with any specific short-circuit capacity in mind. Cable designers in the past have taken very little account of the requirement of short-circuit capacity.

It would be possible, particularly from the point of view of robustness, for the designers to improve the short-circuit rating of the cable. For example, Mr. P. M. Hollingsworth in the discussion points out that the short-circuit rating of an aluminium-sheathed cable can be up to twice that of the equivalent lead-sheathed cable, owing to the increased mechanical strength of the aluminium sheath. If there were a real requirement for increasing the short-circuit rating of a cable it should be possible to improve the mechanical strength. We could, for instance, improve both the tensile strength of the conductor and the resistance of the sheath to bursting.

Mr. A. G. Thomas: I hope that future tests will include aluminium cores and aluminium sheaths. Meanwhile, I invite the authors' comments upon the following calculated figures.

I assume that it is permissible for aluminium cables to have the same temperature rise as copper cables, and this means that an aluminium cable will have a short-circuit rating of 65% that of a copper cable of the same physical size. If aluminium cable is larger, so that it has the same resistance as the copper cable, it will be able to carry a higher short-circuit current. It will also carry a higher current before bursting. If an aluminium sheath is used, its greater strength seems certain to move the bursting limit far enough upward as to be beyond relevance. A Continental cable of $0\cdot19\,\text{in}^2$ aluminium, with an aluminium sheath, has withstood 46 kA for 0·2 sec without bursting, although the insulation was charred.

The current-carrying capacity of an aluminium sheath to B.S. 480 is about 3·5 times that of lead, whilst a corrugated aluminium sheath is 1·75 times. Table A shows the successive limits imposed by either sheath or conductor on 11 kV single-core cable. Improvements in performance are made either by selecting a larger cable (moving downwards), or by changing materials (moving to the right).

Table A

LIMITS IMPOSED UNDER EARTH-FAULT CONDITIONS ON SINGLE-CORE 11 KV CABLES

Size of single-core cable 11 kV	1st limit	2nd limit	3rd limit	4th limit	5th limit
in²					
0·0225	Al cond. 1·2 kA	Pb sh. 1·8 kA Cu cond. 1·8 kA	—	—	—
0·04	Al cond. } 1·9 kA Pb sh. }	Cu cond. 3·0 kA	—	—	—
0·06	Pb sh. 2·0 kA	Al cond. 3·1 kA	Al ½ Sh. 3·5 kA	Cu cond. 4·8 kA	
0·10	Pb sh. 2·4 kA	Al ½ sh. 4·2 kA	Al cond. 5·2 kA	Cu cond. 8·0 kA	
0·15	Pb sh. 3·1 kA	Al ½ sh. 5·4 kA	Al cond. 7·1 kA	Al sh. 10·8 kA	Cu cond. 11 kA
0·20	Pb sh. 3·4 kA	Al ½ sh. 6·0 kA	Al cond. 11·0 kA	Al sh. 12·0 kA	Cu cond. 17 kA
0·25	Pb sh. 3·8 kA	Al ½ sh. 6·7 kA	Al cond. 13·0 kA	Al sh. 13·3 kA	Cu cond. 20 kA
0·30	Pb sh. 4·7 kA	Al ½ sh. 8·2 kA	Al cond. 15·0 kA	Al sh. 16·4 kA	Cu cond. 23 kA
0·40	Pb sh. 5·1 kA	Al ½ sh. 8·9 kA	Al sh. 17·8 kA	Al cond. 19·5 kA	Cu cond. 30 kA

Order of Merit: Sheath. Lead; corrugated aluminium; plain aluminium; plain aluminium plus aluminium armour.
Conductor. Aluminium; copper.

Basis: Conductor. Fig. 1 of Mr. Buckingham's paper amended for aluminium.
Sheath. Fig. 4 of Mr. Buckingham's paper amended for aluminium.

Plain aluminium sheath of same cross-sectional area as lead. Both to B.S. 480 : 1954.
Corrugated aluminium sheath of half cross-sectional area of lead.
Currents are values for 1 sec.

The Solidal cable, with p.v.c. insulation, has been the subject of direct tests, and these show that the cable cannot be burst by any practical current the cable can withstand thermally. The thermal limit imposed by burning or distortion of the insulation under short-circuit conditions seems to be considerably above 160° C. Tentatively, therefore, it seems reasonable to assume that the same temperature limit of 160° C can be allowed, which means that the rating of the cable is the same as that for an aluminium paper cable so far as currents flowing for a few seconds are concerned.

Mr. A. Morello (*Italy*): My recent paper* on the subject is not confined to one type of cable; it discusses all types including the very-high-voltage ones, but it has not the wide experimental background of the paper by Messrs. Gosland and Parr.

There is close agreement on many points between the two papers, but on two points the conclusions are quite different:

(*a*) The temperature limit for lead sheath is fixed at 250° C in the authors' paper, whereas it is 160° C in mine.

(*b*) From the viewpoint of electromagnetic forces, I suggested short-circuit currents of roughly half the values suggested by Messrs. Gosland and Parr.

* MORELLO, A.: 'Condizioni di corto circuito nei cavi per transmissione d'energia', *L'Elettrotecnica*, 1959, 46, p. 814.

So far as (*a*) is concerned, I do not consider a temperature of 250° C safe enough, even taking into account the small alleviation due to heat loss from the sheath. The value of 160° C suggested in my paper is undoubtedly conservative. I pointed out, however, that a reduction in sheath current occurs, for any earthed sheath, owing to earth resistances. In fact, these resistances often attain a value of 20 ohms or more.

With regard to (*b*), my method takes into account the unavoidable fall in mechanical characteristics caused by ageing of the dielectric. In my opinion, the working formula suggested by the authors is suitable for cables having low service temperature, but is questionable for cables designed for high operating temperatures. Similarly, my method does not take into account the mechanical contribution of the lead sheath.

I would therefore recommend, on the grounds of safety, intermediate values, which will be generally suitable for most practical cases. Tests carried out in the laboratories of the company with which I am associated have shown that no modification occurs in the lead sheath up to 175° C (1 sec). A temperature of the order of 200° C or more, however, may introduce structure modification.

In eqn. (3) of the paper I found that the numerical coefficient is based on a lead resistivity of $1 \cdot 22 \times 10^{-5}$ ohm-in, which corresponds to a temperature of 120° C. This value cannot be regarded as 'average' for temperature rises from 50–60° C up to 250° C. I suggest an increase in the mean temperature to 160° C, i.e. a resistivity of $1 \cdot 36 \times 10^{-5}$ ohm-in. The numerical coefficient will then be reduced from 19 to 18.

Mr. J. Solomon: I agree with the recommendations in Section 4.2 of the paper by Mr. Buckingham for 3·3 kV and higher-voltage installations, bearing in mind the fault levels of 150 MVA and above in modern power-station auxiliary systems. There are, however, many installations running quite satisfactorily with cable cross-sections less than the minimum of 0·2 in^2, which may be explained to some extent by the tendency to have regard to the economics of a particular installation. This attitude is encouraged in a recent article in a Belgian journal, which suggests that the capitalized value of the losses should also be taken into account whenever a cable is expected to run effectively for more than, say, 1 000 hours per year.

With regard to medium-voltage switchboards, oil circuit-breakers are seldom used nowadays for distribution in large power stations; modern practice favours metalclad air circuit-breakers. The smaller (and usually less vital) circuits consist of switch-and-fuse equipments, but motors in the range of, say, 50 to 250 hp can be controlled by fused-contactor circuits, so that the author's recommendations fit in very well in these instances.

However, where circuit-breakers are installed, it would be an embarrassment to have to add back-up fuses to the modern type of metalclad equipments, particularly where, in order to save floor space, they are of 2-tier construction. There may also incidentally be some inconvenience where one of the three fuses in a circuit remains intact after a 3-phase short-circuit. Fortunately, the sizes of cables associated with circuit-breaker equipments line up fairly well with those recommended in the paper, and, in general, our operating experience with 415-volt power-station cables has not been disquieting.

Mr. H. Lloyd-Williams: It is not clear whether the authors have allowed for the full short-circuit current to return along the particular cable sheath concerned. It has been mentioned that aluminium sheathing would probably overcome the difficulties due to heating. In about 1949–50, owing to shortage of steel for armouring, we were forced to use aluminium armour on a number of occasions, and it became clear that the lower resistance and lack of inductance of the aluminium armour was 'stealing' the return current. In one case we lost the aluminium sheathing in a number of places as the result of a short-circuit.

Mr. N. Barnes: If reliance is placed on armouring wires to carry a proportion of the fault current, special care must be paid to bonding at exposed terminations and to protection of the wires from corrosion. In an endeavour to achieve the latter, I have specified p.v.c. sheathing over the armour, but I should welcome the authors' recommendations on reliable bonding.

Bonding of armouring wires by bedding them on a metallic braid and clamping them between a split steel ring and a steel clamp tightened by a small screw jack can give a satisfactory connection initially, but insufficient time has elapsed for me to decide whether this form of connection will give lasting satisfaction.

Mr. C. T. W. Sutton: Some years ago, no doubt in emergency, some shrewd cable engineers produced a method of calculating the short-circuit capacity of a cable. A value of 120° C was taken as the maximum permissible conductor temperature, with an additional safeguard that no alleviation was permitted for the dissipation of heat. This value was probably derived from the temperatures of the drying and impregnating cycle, after which it could be guaranteed that no deterioration of the dielectric resulted. The procedure, which is simple and direct, has worked well, but criticism has been made that the method is conservative. Investigations were therefore made by the E.R.A., resulting in the usual excellent report we always obtain from them.

The conclusions indicate that permitted short-circuit capacities depend upon a number of factors. Consequently, the situation is now more complex than hitherto and the determination of short-circuit capacities is liable to argument and adjustment.

It is unfortunate, therefore, that more positive recommendations have not been made in Mr. Buckingham's paper.

Messrs. Gosland and Parr list the maximum short-circuit temperatures adopted in other countries. Would it not be possible to recommend a simple solution, e.g. 160° C on the conductor, at least for wire armoured cable? This would not create serious risk or increased cost for operating conditions now used. Perhaps more positive recommendations will be forthcoming when the investigations into screened cables are completed. Have the authors any views on the effect of repeated short-circuits on a cable structure?

Dr. G. Mole (*communicated*): The experimental investigation described by the authors relates entirely to the properties of cables in relatively new and unused condition. It is instructive, therefore, to examine the results with a view to determining whether the conclusions require modification when considering cables which have been in service for a period of years. It appears that the mechanism most effective in reducing the permissible short-circuit rating will be reduction in the bursting strength of the belting papers by chemical ageing. A similar mechanism acts to reduce the mechanical strength of transformer windings on short-circuit, and an informative survey of the relevant properties of paper has recently been carried out by Fabre and Pichon.*

From this it is clear that, under the conditions of operation in a cable, any ageing of the papers which occurs will result in a reduction in their tensile strength and will be dependent mainly on the moisture content and operating temperature. The most onerous conditions of use are those where the cable runs continuously at the maximum current prescribed in the E.R.A. rating tables, and only cables having a conductor section of 0·3 in^3 or more, rated at 6·6 kV and under, will be concerned. For such cables the temperature of the belting papers may be

* FABRE, J., and PICHON, A.: 'Deterioration Processes and Products of Paper in Oil. Application to Transformers', C.I.G.R.É., Paris, 1960, Paper No. 137.

estimated as 70–75° C, assuming the thermal resistivity of the soil in which they are embedded to be 120° C cm/W. The time required for the tensile strength of the belting papers to fall to 50% of its initial value may now be calculated and is given in Table B.

Table B

Rated voltage	Conductor temperature	Estimated temperature of belting papers	Moisture content	Time required for tensile strength to fall to 50% of original value
kV	°C	°C	%	years
1·1			0·3	300
3·3	80	70–75	1	70
6·6			3	14

In considering these figures, it has to be borne in mind that the initial tensile strength will vary with moisture content. Nevertheless, the results of the calculation are reassuring, since it is evident that lives of less than 70 years are to be expected only for certain cables run under the most onerous conditions permitted and having levels of moisture content higher than those dictated by consideration of the dielectric properties of the insulation.

Mr. T. J. Rowlands (*at Birmingham*): With reference to Section 3.1 of the paper by Mr. Buckingham it is not clear how a 4-core 0·3 in^2 p.l.y.s.t.s. cable is found to be safe at 25 MVA for 0·10 sec. Fig. 6 must presumably be applied, and one finds that the bursting current is about 28 kA. Low-voltage oil circuit-breakers are in service in very large numbers in works substations, and they have a very good performance history. Only in a very small proportion has consideration been given to the effect of short-circuit faults on cables connected to these equipments, and the small number of failures indicates that the commercial risks have been small. Circuit-breakers have many advantages to offer over h.r.c. fuses in certain parts of works installations, and it would be quite wrong to overlook past records when selecting cables.

It is unfortunate that in Section 4.1 the author should have restricted his criticisms to oil circuit-breakers. An increasing number of air-break circuit-breakers are going into service, and it is only right to state that these are not inherently able to provide faster or more reliable operation than oil circuit-breakers. The reliability record of both types is extremely good.

Mr. J. T. Henderson (*at Glasgow*): The proposed maximum conductor temperature of 160° C, although appreciably higher than the previously accepted temperature of 120° C, still seems low compared with the values accepted by other countries. While the higher figures quoted may conceivably be justified if either the design of cable components, installation conditions or jointing techniques were fundamentally different, these differences would not appear to be sufficiently pronounced to explain the considerable discrepancy between the permissible maximum conductor temperatures. Perhaps the basis on which foreign countries have calculated this value differs from that quoted in the paper.

In Section 1 of the paper by Messrs. Gosland and Parr, constants are given for 15° C, 60° C and 85° C. The final figure seems illogical. Is there a possibility of an error in this figure?

Armour and sheath temperatures of the order of 250° C would seriously affect certain types of present-day anti-corrosion beddings and servings. No consideration seems to have been given to this.

In the penultimate paragraph of Section 6 it is stated that the discharge magnitude, although increasing immediately after short-circuit, resumes, in time, approximately its previous level. This is claimed to be due to the redistribution of compound with time. While this will undoubtedly occur with standard oil/rosin compound impregnated cables, it is very doubtful whether any redistribution will take place in cables impregnated with compounds loaded with microcrystalline wax. These are now being used in the manufacture of mass-impregnated non-draining cables.

Mr. F. Mather (*at Manchester*): It is necessary to view the question of short-circuit rating of cables in proper perspective, and the following statistics for one Area Board may assist. In a typical year there are 300 faults on high-voltage cables, i.e. one per 20 miles per year. Of these, 80 are due to mechanical damage, 70 to insulation failure, 50 to termination failure and 40 to joint failure. When these failures occur the cables are subjected to short-circuits of widely varying degrees of severity, and a proportion of failures are undoubtedly due to the delayed effects of previous short-circuits. It is seldom that a cable is completely destroyed by a short-circuit, but it is probable that the unreliability of certain cables is largely due to their short-circuit history.

Messrs. Gosland and Parr appear to accept present designs of compound-filled joints as a major limiting factor, but it would seem quite practicable to remove this limitation by the use of hard-setting resins.

Trifurcating boxes are a problem, and whilst the usual cable bend below the box may relieve mechanical stresses within the box, it would appear to concentrate the stress at the bend—a point where many failures occur. It is disturbing to see in Section 6 that certain new cables have a discharge inception level of half the working voltage to earth. There are instances where 11 kV cables have become unserviceable after four or five years owing to the effect of internal discharge.

Mr. Buckingham states that 'distribution engineers must be prepared to lay cables large enough for the prospective short-circuit duty'. It is, however, costly to increase the conductor section, and perhaps more thought should be given to reduction in operating time since unit protection will give clearance in 0·15 sec, and even four stages of time-delayed protection only require about 1·5 sec.

With regard to the use of screened cables for 11 kV service, it would be of value if statistics could be made available to show that earth faults are prevented from becoming phase faults, and that the overall fault incidence is lower than with belted cables.

The value of fuses for protecting existing cables of small section might be emphasized.

On medium-voltage public supply networks there is virtually no trouble due to short-circuit currents, and the main difficulty is to ensure that, when faults occur, sufficient current will flow to operate the distributor fuses.

It would be interesting to have the authors' opinions as to whether plastic-insulated mains cables are likely to prove as trouble-free as paper-insulated cables under short-circuit conditions.

Mr. L. M. Sloman (*at Manchester*): Between 1945 and 1952 a series of E.R.A. Reports were issued concerning the short-circuit ratings of paper cables based on the charring temperature of the dielectric. The recommendation of these Reports that the short-circuit level be based on a figure of 100 kA/in^2 for 1 sec was not accepted by most cable makers, and their reluctance has been fully justified by the results of the various experiments described in the paper by Messrs. Gosland and Parr.

The bursting tests described in Section 4.1 were all carried out on cables with the sheaths removed or with plain lead-sheathed cable. Do the authors consider that eqn. (7) should be modified

for a served or armoured cable? Furthermore, the effect of electromagnetic forces on single-core cables has not been investigated, and the effect on these cables under symmetrical fault conditions is liable to be more damaging than on 3-core cables. The authors make no observation on the use of screened 11 kV cables, which are becoming more widely used, and the absence of a paper belt will reduce the resistance of this type of cable to electromagnetic forces. The use of a metal tape as a screen is to be preferred to the usual metallized paper to reduce the possibility of phase-to-phase faults.

Mr. L. Wain (*at Manchester*): An incident occurred recently on a 6·6 kV distribution system when a 0·06 in^2 6·6 kV feeder was called upon to carry a load of 250 amp. The feeder was derated to 100 amp (continuous rating) for 40 yd outside the first substation. After three hours the feeder tripped out and two joints were found to be faulty, the damage being very extensive after a short-circuit current of 8·3 kA had flowed for 0·9 sec.

Subsequent examination of the remaining joint, which had not broken down and had not been subjected to the latter short-circuit owing to its being beyond the fault, revealed bad design, compound migration and voids in the sleeve, and some distortion of the cores.

After the faulty joints had been repaired the circuit was switched in again from the other end of the feeder, the short-circuit current being 2·5 kA for 0·9 sec in this case, and a cable fault then occurred in the above-mentioned 40 yd section. Apart from the damage at the point of fault, examination revealed distortion of the lead in the form of a spiral hump, burst belt insulation and badly-charred core papers.

It would be interesting to have the author's views as to how much the overload condition contributed initially towards the damage mentioned compared with the effects of the high short-circuit current, which was admittedly considerably in excess of Mr. Buckingham's permitted figure of 5 kA for 0·9 sec.

Mr. R. H. Hayward (*at Manchester*): The paper by Mr. Buckingham indicates that cable circuits may fail due to various causes under short-circuit fault conditions. Failure of joints and sealing ends is mainly due to the conductors expanding with rise of temperature, whilst sheaths fail mainly due to the difference between conductor and sheath expansion ratio.

As a layman in accessory design, I wonder whether making greater efforts to withstand this expansion by strengthening joints, etc., is the best approach.

A new approach to jointing might be a compression ferrule made in two concentric halves, which, when longitudinally compressed by the expanding conductors, would reduce in length, thereby preventing the joint destruction due to compressive forces. In the sealing ends, could not the conductor expansion be accepted by flexible connections as in 33 kV sealing ends? In Grid substations the expansion of busbars is catered for rather than repressed.

The figures in the paper are most useful as a guide to safe practice. It is worth noting that these represent worst conditions, since fault impedance will reduce the maximum fault current. Cases will arise where a cable of adequate load-carrying capacity but inadequate fault capacity is involved, and increased cable size is proposed. The increased annual charges can be calculated. The designer is then left to assess whether the extra cost can be economically justified.

The system design engineer uses his experience to guide him on the question of justifiable risks. A paper dealing with the analysis of cable-system faults might be very helpful in assessing on a numerical basis what is an economic risk in cable circuits.

Mr. A. G. Milne (*at Cardiff*): Quite apart from the valuable up-to-date information and conclusions in the papers, they are of value because they make one take stock and reappraise the effects of increasing fault duties on systems.

In practice, there has been singularly little trouble from this cause, and Mr. Buckingham acknowledges this with the explanation that most faults start as resistance-limited earth faults and that high-speed protection clears them. Nevertheless, fault levels are increasing and it is wise to heed the advice given.

For instance, reference is made to an increase in the conductor size above that which is necessary to carry the load. As there has been so little trouble in practice, it is doubtful whether a case can be made for the extra capital that would have to be found. Would not the money be better spent on improved maintenance to ensure that the protection operates? This would not only protect cables, but would also confer benefits on all the other components of the system. In general, the greater problem is under-running cables and not utilizing them to an economic optimum.

I agree with the use of steel-wire armour, but the author's qualification of corrosion trouble is all too true in practice. Indeed, the serving is surely designed to permit intimate contact of the armouring with earth to improve the earth-fault path. In these conditions corrosion seems probable.

To prevent this, it might be advisable to sheath the armouring with p.v.c. As this would only be necessary for cables in high fault zones, the cost would not be a significant factor.

The recommendation to use single cores to avoid phase-to-phase faults might cause some trouble at major substations in rural areas, as high-resistance earth faults might cause leaks to remain undetected for lengthy periods. Three-core screened cables would be preferable in such cases.

The author recommends the raising of 11 kV fault levels to 250 MVA. In Bristol this has been the case for the past 20 years, and in two major substations with 6·6 and 11 kV outgoing, the level is 350 MVA. Bristol has a very extensive h.v. cable system, and despite the high fault levels, in all the years of operation only two cases of damage due to high fault duties have occurred.

In one case, severe charring of paper on a 0·1 in^2 11 kV cable occurred on a 3-phase short-circuit in a 300 MVA fault zone. Protection operated in 0·10 sec.

In the second case the cable was smaller, namely 0·0225 in^2, and it suffered severe damage when called upon to pass a 300 MVA fault current. This incident occurred in 1943 and provided an opportunity for the E.R.A. to attempt an investigation, although the extent of the damage left little evidence.

Clearly both cables were too small for their position in the system, but in a rapidly growing and extensive cable system this was perhaps understandable.

Despite this freedom from trouble, the papers show that our old design conceptions should be looked into, and it is not unlikely that it may be necessary to consider the installation of larger-size cables in high fault zones.

Mr. J. Mitchell (*at Cardiff*): Messrs. Gosland and Parr have not considered expansion of the compound, which may contribute to the bursting of the belt papers, or expansion of the lead sheath permitting ionization in voided regions, particularly on h.v. cable.

The term $F/\sqrt{3}$ developed for the bursting of a 3-core cable might be modified to $F/2\sqrt{2}$ for a 4-core cable with some advantage.

The inclusion of a further curve in Fig. 19, to show the present accepted rating, would have provided a useful comparison and also shown the vulnerability of unarmoured and steel-tape-armoured cable to earth faults.

The paper shows that for most distribution cables the joint is the weakest link, and the mechanism of failure seems similar to that in the tests described in the C.I.G.R.E. Report No. 201,

1960, for cable laid in ducts in which the frictional resistance considerably restrained the cable movement. It is possible that, for long lengths of cable, the movement of the core into the joint may be less than that determined by calculation.

Most cables operate below their maximum temperature, and it is probable that short-circuits do not develop temperatures in excess of 105° C with a corresponding temperature rise of about 75° C above stress-free conditions. The suggested rise of 120° C will thus make comparisons difficult. In broad detail, two short-circuits under the present rating would be equivalent to one under the proposed rating. The reclosing of circuit-breakers after a fault to 160° C requires careful consideration, as, for instance, a 1·0 in² 11 kV cable would require about 45 min to cool to its maximum operating temperature.

The present work should be continued to include screened-type cable where the advantage of the belt papers, which provide about 50% of the bursting restraint, is not available.

Mr. G. O. Romans (*at Gloucester*): In a case of damage to a 6·6 kV cable some years ago, which was due to a protection failure, it was noted that a high-pressure jet of oil and compound had been ejected from the cable end boxes. The paper by Messrs. Gosland and Parr does not mention the effect of cable-oil expansion, but it does seem that the effect could be significant in assessing the bursting limit of a cable.

Reference is made to soldered ferrules imposing a temperature limit of the order of 160° C. Since experiments are taking place with compression fittings, especially in m.v. work, I wonder whether a significant improvement on this account might be achieved in certain circumstances.

Section 2.2 of the paper by Mr. Buckingham refers to cables with larger conductors than are necessary to carry the load having the stress-free temperatures lowered. I agree that the increased temperature rise available before 160° C is reached should not be taken into account, because of the increased danger of buckled joints. This will, of course, apply to the majority of cables for distribution work, since they will usually be installed initially with some spare capacity for future growth of load. Presumably the stress-free temperature will gradually rise until full continuous load or its equivalent on a cyclic basis is reached.

I should like to stress the point made in Section 2.1 of Mr. Buckingham's paper. It is quite probable that one will have the combination of old small-section cables and armour wires in bad condition, and therefore considerable caution in calculating a short-circuit rating is necessary.

The papers, of course, refer to cables with unscreened cores. Without anticipating too much the contents of future papers, it would be helpful if the authors could give some idea of work which may be progressing on these cables, and whether any pointers can be given at this juncture.

Mr. W. Hill (*at Gloucester*): I note the possible change in rating of cables and Mr. Buckingham's suggestion that feeders running out from a substation can be graded into conductor size so as to meet the point that fault currents diminish fairly rapidly with distance from the primary substation.

I wonder whether this is the correct procedure to adopt in view of the rate of development which is occurring. Prior to nationalization, Cheltenham only had one primary substation; there are now two, and a third is being planned. At Gloucester prior to nationalization there was only one; there are now four, a fifth is definitely planned, and there is a prospect of a sixth.

With the planning of further primary substations the direction of feed can be reversed, and at times we have been embarrassed by small cables in outlying parts when installing another primary substation. More notice should be taken of the general level of fault values to allow for development, rather than just dealing with the immediate situation.

Mr. F. Williams (*at Gloucester*): The papers indicate that conductor and sheath temperatures under short-circuit conditions are the limiting factors, and the dielectric has a higher safety factor.

Was the investigation carried out on cables impregnated with an oil resin compound, i.e. mass impregnated as we knew it some 10–15 years ago, or were they the type we now know as m.i.n.d. cable?

Have any experiments been carried out on cables installed possibly 20 or 30 years ago? What effects occur owing to deterioration of the dielectric strength due to migration of compound, and other factors that have to be considered?

Mr. J. J. Harris-Clarke (*at Gloucester*): In Section 2.1, Mr. Buckingham gives the figure of 2 kA or less as the normal-practice limit of the earth-fault current. Would he comment on that figure and give the reasons for its adoption?

In the late 1930s earth-fault current was limited on the 11 kV network of one undertaking to 250 amp by 30·4-ohm liquid resistor. In the early 1940s Howarth* quoted 300 and 450 amp as the figures adopted by the Lancashire Electric Power Co. These have since risen, and one Area Board recommends 1 kA as the limit for larger transformers and solid earthing for 5 MVA or smaller transformers on rural networks.

With the rapid development of the supply industry and the efforts of standardization necessarily resulting from this, one may tend to overlook the fact that soil resistivity is a deciding factor in determining the earth-fault current available and hence the liquid resistance. Possibly there is a case for different neutral resistances for underground networks and overhead ones, and I should like to have the author's views, in the light of statistical information, on system performance available to him.

There is some interesting information available from the Texas Electric Service Co.† They normally limit the phase-to-neutral fault current to 3 kA. This is accomplished by a combination of high-impedance transformer and reactor in the neutral. In West Texas, tests with a span of conductor lying on the ground resulted in only 40 amp of earth-fault current, and a staged fault produced 100 amp when the phase conductor was connected to a standard 8 ft earth rod.

Mr. R. P. Townsend (*at Newcastle upon Tyne*): The introduction to the paper by Messrs. Gosland and Parr makes it abundantly clear that the time has come for some revision of our ideas on the short-circuit ratings of paper-insulated cables up to 11 kV. It is unfortunate that the investigation is limited to faults not exceeding 3 sec, since the only instances of which I know at present of cables being burnt out over some distance have been caused by what is perhaps nearer to a sustained fault overload of considerable duration rather than by short-time faults.

It would have been appreciated if both Figs. 1 and 3 had been extrapolated, since it is felt that both might have been of use, the former in connection with possible very-short-term overload ratings and the latter in connection with minimum reclosing times for use by operating departments.

My only disagreement with the authors is in connection with their statement in Section 3.1 that the sheath was clamped tightly to the core 'thus, as in-service, preventing relative movement'. These comments related to single-core cables and many national standards for terminations of single-core cables, e.g. transformer secondary leads, employ flexibles specifically designed to allow such movement.

Many of the tests on low-voltage cables on the bursting effect

* Howarth, O.: 'Protective Systems for Supply Networks Operating at Voltages up to 11 kV', *Journal I.E.E.*, 1944, 91, Part II, p. 6.

† Guenzel, E. L., and Morris, W. T.: 'Distribution Circuit Protection'.

of electromagnetic force were made on 3-core cables. As 4-core cable is in more common use it would be interesting to know to what extent the results are directly applicable and also whether tape armour, which is commonly used to protect these cables, would contribute anything to their resistance to bursting.

One of the main limits on short-circuit carrying capacity of cable, as proposed by the authors, is imposed by the need to prevent distortion of the joints owing to expansion of the conductors into them. While expansion joints which have been successfully used in areas of ground substance are less desirable than normal joints, there would seem to be a case for using them under certain circumstances in order to obtain even higher short-circuit ratings than those envisaged, subject of course to the other restrictions mentioned.

I should welcome fuller details on some of the test rigs employed, and, in particular, I should be glad to know whether, in the expansion tests, the cores were permitted to twist.

In Fig. 17 a common line has been drawn for all cables, but, while a more steeply rising characteristic would fit the 1·1 kV cables, a more horizontal line would fit in better with the 11 kV range. In view of the difference in the ratio of paper to copper, size for size, between these two groups of cables, one might perhaps expect differing characteristics.

Mr. Buckingham's paper is very welcome as a practical interpretation of the paper by Messrs. Gosland and Parr, and one must agree with his comment that much work has still to be done on screened-type cables and those with aluminium conductors, etc., before the results are applied to such cables; in particular, in the case of screened cables, the adequate conductivity of the metal screens would seem to require close attention.

It is not clear from the paper, particularly in view of his recommendation that single-wire armoured cables should always be used where short-circuit energy level is high, why so few cable sizes are shown in Fig. 2. I should also like to know why bursting currents are indicated alongside those diagrams which relate to earth-fault currents, since the investigation by Messrs. Gosland and Parr into bursting currents seems to have been confined to the phase-to-phase condition.

Would the author explain which statutory limitation he was referring to in Section 2.2?

Mr. R. R. Pattinson (*at Newcastle upon Tyne*): Whilst the belted type of cable was no doubt the correct choice when the tests were first programmed, the preference is now for screened cable, and the results of the forthcoming tests on these will be awaited with interest.

The significant part played by armoured wires in carrying fault current raises the same question on the reinforcement tapes of pressure cables. Since expensive imported materials are involved it would seem wise in principle to specify that the sheath, reinforcing tapes and armour be considered to carry their share of fault current.

Fault currents are reaching such high levels that the question whether cables shall have a declared short-circuit rating requires careful examination, as also does the kindred subject of duration times. Modern protection and circuit-breakers can clear faults in about 0·2 sec and this time could well be taken as the minimum. Care, however, must be exercised when specifying durations of 1 sec or longer, otherwise the cost of the cables may be significantly increased. In fact, it may well prove the more economic for long feeders to duplicate the high-speed main protection as back-up than to increase cable sheath thickness to withstand the longer fault duration.

It is doubtful with pressure cables whether the sharing of the fault current between sheath, reinforcement and armour is amenable to precise calculation. The alternative would seem to be to type-test these cables under short-circuit conditions at a specified fault level. Whilst present test facilities would render this proposal impracticable for all projects, the economies resulting from careful cable specification would justify this approach on selected major cable schemes.

Mr. L. A. Bates (*Newcastle upon Tyne*): The investigations relate to belted-type cables, but the alternative of screened multicore cables is available at 11 and 22 kV, and some reference is made to this in the paper. It would seem that the absence of the restraining belt would considerably reduce the resistance to bursting in the case of the screened cable, although this would be offset to some extent by the greater axial separation of the cores. Would the authors express an opinion on whether the limitations due to bursting are likely to be to a much greater degree the deciding factor in limiting the short-circuit current in cables of this type?

In multi-core cables the limit of earth fault current does not bear a consistent relationship to the conductor short-circuit current, but varies somewhat fortuitiously.

In the case of 4-core 1·1 kV unarmoured or tape armoured cables the relationship varies from 105% of the conductor short-circuit current in the 0·04 in.^{2} size to approximately 45% in the 0·3 in.^{2} size. In corresponding wire-armoured cables of this voltage, the relationship varies from 300% of the conductor short-circuit current in the 0·04 in.^{2} size to 110% in the 0·3 in.^{2} size.

If it is considered worthwhile to cater generally for an increased earth-fault limit in these cables as well as increasing the earth-fault current by reason of the increased conductivity of the metallic coverings, there seems a reasonable case for the more general adoption of effectively-bonded wire-armoured cables for l.v. distribution. The use of these would still provide adequate mechanical protection, would simplify jointing processes and may also allow a reduction in lead sheath thickness, if this were also permissible from other considerations.

The results relate to paper-insulated cables impregnated with the conventional resin/oil compound. Are any of the factors likely to be significantly influenced by the use of 'non-draining' type compounds, which have a much higher viscosity at normal operating temperature and probably much lower viscosity at temperatures above 120° C?

Messrs. L. Gosland and **R. G. Parr** (*in reply*):

Effect of Armour on Bursting Strength.—We have confirmed that wire armour contributes significantly to the resistance of belted cable to bursting, but have not explored the limits which already, even with the inclusion of a factor of safety sufficient to cover possible ageing, seem unlikely to be restrictive. It is proving necessary to examine the contribution of armour more carefully for screened lead-sheathed cables, which are less robust in this respect; the results should be applicable to belted cables. Bursting limits for aluminium-sheathed cables will be higher than those for lead-sheathed cables of similar dimensions. We think it unlikely that bursting limits for higher-voltage cables will be as onerous as those for cables up to 11 kV of similar type.

4-Core Cables.—The sheath of shaped 4-core cables may be assumed to distort into an approximately square shape before bursting. Under these conditions the bursting current is given with sufficient accuracy by eqn. (6).

Single-Core Cables.—We have not considered the bursting limits of single-core cable systems because they are determined by methods of installation and not by the cables themselves.

Armour Clamps.—We have not damaged any armour clamps in our tests, but all used were, of course, assembled with great care. Armour clamps should be robust with ample current-carrying capacity and have a degree of flexibility in order to

make effective contact with all the armour strands. The maintenance of good contact over long periods, including heating cycles and short-circuit heating, is facilitated by using a resilient assembly and avoiding small highly stressed parts for transmitting contact pressure.

Effect of Type of Compound.—All our work was on cables impregnated with oil/resin compound. We think it unlikely that any of the limits proposed would be significantly different for m.i.n.d. cables. There was evidence of gas pressure having an effect on belt papers and on the sheath, but only at conductor temperatures well above the limits proposed.

Joints.—Suggestions have been made that limits could be raised by the use of rigid filling or expansion joints. Changes proposed with the object of increasing the limits in a particular respect require consideration from a number of angles outside the scope of the paper.

The limits set by longitudinal expansion in stressing the joints are unlikely to be altered by the substitution of an aluminium sheath for a lead one, since the values proposed already assume an extremely rigid environment.

Short-Circuit Limits set by Sheath and Armour.—Mr. Morello suggests that the limit we have adopted for the temperature of the lead sheath is too high. We do not think it necessary to reduce this figure for the class of cables dealt with, having regard to the object of the paper. We agree that sheath currents in service are affected by many factors not completely under control. The points made by Mr. Bird are highly relevant in this respect.

Repeated Short-Circuits.—There would appear to be no reason why a cable installation should not withstand repeated short-circuits, provided that the limits given are respected and that successive short-circuits are separated sufficiently in time for the cable to return to its normal temperature and state of mechanical stress.

Stress-Free Temperature.—It is necessary to distinguish clearly between the stress-free temperatures quoted in Section 5.10, which are those appropriate to cables in normal service, and those in Section 5.9, which are appropriate to particular cables in particular test conditions.

Cables in Air.—Considerations relevant to cables in air are set out in References 9 and 10.

P.V.C.-Insulated Cable.—Work to date suggests that p.v.c.-insulated cables are likely to have satisfactory short-circuit performance, but that the question of sustained overload as distinct from the short-circuit condition requires close examination. The problem of making and maintaining a satisfactory armour connection is important with these cables.

Particular Cases.—The example of a cable failure under gross overload, cited by Mr. Wain, is of interest in that the temperature rise of the conductors was probably well in excess of 120° C, and mechanical damage to the joints is therefore to be expected, particularly if the joints are of older design. A fault of 8·3 kA for 0·9 sec would, of course, severely damage the insulation of the cable and cause further damage in the joints.

We suspect that the regular bursting mentioned by Mr. Bird may have been due either to relief of longitudinal expansion in a long cable or to effects of the pressure of gas vapourized from the impregnant, provided that the fault duration was sufficient to give a temperature well in excess of 160° C.

Generalized Limits.—We have some sympathy with Mr. Sutton's proposal that a single limit of 160° C be adopted for wire-armoured cables, giving the same convenience and ease of application as the C.M.A. figure of 120° C, the origin of which Mr. Lythgoe so well describes. Reference to Fig. 19 (*c*) shows that this would, in effect, be satisfactory for 11 kV cables and for a large number of 1·1 kV cables, all designed to B.S. 480: 1954. There would, of course, be danger if such a rule were universally applied.

We regret that limitations of space prohibit a complete answer to all the excellent points raised. Many, and particularly those on experimental detail, bear on topics which are fairly fully discussed in References 8–10.

Mr. G. S. Buckingham (*in reply*): A number of speakers suggest that the figures of safe short-circuit currents present a gloomy picture and that recommendations are made that unnecessarily large sizes of cables should be used. Some speakers refer to the long and satisfactory lives of cables subjected to normal hazards of overloads and faults, and others give details of damage to cables in similar circumstances. The researches of the E.R.A. and the curves reproduced in the paper have endeavoured to place on a more secure foundation the ability to assess the true causes of damage in these cables. For example, some references have been made to the bursting of cables at low fault currents. It must now be clear that these incidents are more likely to have been caused by rapid—even explosive—expansion of vapour pockets trapped within the cable.

Several references have also been made to the period of time during which fault current should be permitted to flow. The 3 sec limit is based on the fact that under modern conditions, even with several stages of discriminative protection, it should be possible to disconnect all cables from busbars in that time. There is no need, therefore, to base all calculation on this period, and it is in this field that the distribution engineer must exercise his skill in fixing protection settings in the light of the improved knowledge of the short-circuit capabilities of his cables. This also applies to automatic reclosing of circuit-breakers. It is easy to calculate that in some places the short-circuit level is low enough to enable two or even more reclosures to be made safely, but in others, nearer to main sub-stations, it is not so safe and some degree of risk of damage to cable may be incurred. The new information enables this risk, if it exists, to be evaluated more accurately.

The support for aluminium sheaths is interesting, and it may well be that with improved outer servings becoming available the advantages of this metal over lead as a cable sheath may overcome the present reluctance of engineers to use it for distribution cables.

Messrs. D. T. Hollingsworth, Thomas, Mather and Hayward all make the suggestion that if short-circuit conditions are paramount in the design of a cable network it would not be difficult to modify present designs of mains cables and joints to resist the effects of short-circuits. These suggestions should be borne in mind when national specifications are being revised.

A number of speakers refer to the possibility of using the results of the present researches to amplify our knowledge of other types of cable. It would be better to await the publication of further E.R.A. Reports, and it is hoped that some of these will be published fairly soon.

The contributions of Messrs. Solomon, Rowlands and others in connection with the use of m.v. automatic circuit-breakers in industrial installations are welcomed. The theoretical considerations make it difficult to avoid recommending h.r.c. fuses as back-up protection in places where the short-circuit level is high, but it is reassuring to have so much confirmation that in practice there is very little cause for disquiet.

Dr. Mole's contribution is helpful and confirms that with average stress-free temperatures a long and trouble-free life can be expected from well-made cables.

Mr. Hill makes the useful comment that, in the development of important networks, mains cables may serve different areas during their useful lives. Mr. Bates points out that a case can be made out for the use of single-wire-armoured m.v. cables.

Experience seems to have shown that with h.r.c. fuse protection the cheaper steel-tape-armoured cable has given every satisfaction.

The reference to the fact that single-core unarmoured mains supply cables laid direct in the ground might not comply with statutory requirements was based on a study of the formal approvals given by the Minister of Power and his predecessors under the Electric Lighting Clauses Act, 1899. It would now appear that the approval given on 8th July, 1940, does, in fact, permit the use of single-core h.v. and l.v. cables laid direct in the ground, provided that they are covered with interlocking reinforced concrete slabs or hard-burned tiles to specified requirements. This point was raised by Messrs. Bird and Townsend.

Mr. Harris-Clarke comments on the earthing resistance to be used for limiting phase-to-earth faults, and rightly points out that the soil resistivity will affect earth currents. Standardization is not possible under the varying conditions existing in this country, and each installation must be considered on its own merits.

DISCUSSION ON 'THE APPLICATION OF THE METHOD OF IMAGES TO MACHINE END-WINDING FIELDS'*

Mr. P. J. Lawrenson (*communicated*): In the paper, consideration of air-gap representation is limited to coils spanning π mechanical radians, but in practice it may also be necessary to represent the effect of coils spanning other angles. Treatment of this more general problem requires the insertion of 'fictitious' currents, of *unequal* magnitudes, in the two arcs (divided by the parallel coil sides) of the gap, the appropriate magnitudes of these currents being determined from a consideration of the continuity of flux in the end-region.† It should be noted, however, that the resultant distribution of current in the air-gap, for a complete winding, is independent of the ratio of the magnitudes of the two parts of the air-gap current (due to one coil).

Discussion of turbo-generator rotor-coil retaining rings is restricted to the magnetic variety. This is a misleading emphasis because, in practice, non-magnetic ones are nowadays used almost exclusively, at least in large machines where end-field effects may be particularly troublesome and so justify full theoretical investigation.

Analysis under steady-state conditions of end-fields with non-magnetic rings is, however, simple. Since only the stator-field harmonics induce eddy-currents in the ring, the presence of the ring can be ignored. Further, so far as the stator-current fields are concerned, the rotor shaft, though highly permeable, is relatively small and can be ignored. The main boundary of the field can thus be represented by a single plane surface coinciding, so far as possible, with the stator and rotor ends.

However, so far as rotor-current fields are concerned, the effect of the rotor shaft cannot be neglected, and the main boundaries of the field comprise a permeable cylinder projecting from a plane surface. As Mr. Carpenter points out, the image method yields no exact solutions for the effect of cylindrical surfaces on the fields of three-dimensional current arrays, though several approximate treatments are possible. Of these, I suggest that the best is that using the image formed as the inverse, in the cylinder, of the actual circuit: this image naturally fulfils the conditions of continuity; it gives an exact representation for currents parallel to the cylinder axis; and the approximation for other components of current is simply that the cylinder is replaced by a sphere of the same radius. It is, of course, also necessary to form images in the plane surface of the current and its image in the cylinder.

The discussion in Section 5.2 of the image solution for two boundaries intersecting at right angles, Fig. 9, and the footnote on page 493 have no meaning, since the physical problem is not completely specified; the boundary between the two regions of permeabilities μ_1 and μ_2 is not defined.

The only image solutions applying to the boundary when this has two parts with different values of permeability are for the cases when one surface is infinitely permeable and the other is completely impermeable ($\mu = 0$); or when one half of the whole region has finite permeability and one quadrant has a permeability of zero, unity or infinity (Fig. A). No problem

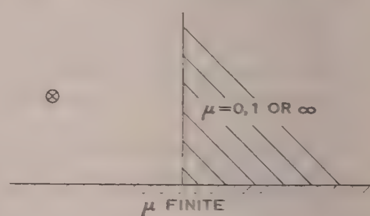

Fig. A

involving any other line of intersection between the regions of different permeabilities can be treated by the image method: its solution would require the formation of images in boundaries either part straight and part curved or wholly straight but intersecting at angles not submultiples of π, and neither of these is possible.

Mr. C. J. Carpenter (*in reply*): I endorse Mr. Lawrenson's remarks on the extension to chorded windings of the equivalent-conductor representation of the air-gap. The rotor end-rings have, as he points out, no effect on the steady-state fundamental-frequency fields if the rings are non-magnetic, and I agree that they can then be disregarded. Under subtransient conditions, however, even non-magnetic rings may have to be taken into consideration.

The qualifications which Mr. Lawrenson has added to the discussion of images formed in surfaces meeting at a right angle are important in principle, and I am grateful to him for having indicated them more clearly than was possible in a brief footnote. The list of possibilities does, of course, include two surfaces having the same permeability. However, I cannot agree that these qualifications make the discussion meaningless. The circumstances in which an exact solution is possible include all those of general practical interest, and where an attempt is made to allow for finite permeability the position of the third interface is unlikely to have a significant effect.

* CARPENTER, C. J.: *Proceedings I.E.E.*, Paper No. 3327 U, October, 1960 (107 A, 487).
† LAWRENSON, P. J.: 'The Magnetic Field of the End-Winding of Turbo-Generators', *ibid.*, Paper No. 3490 S, February, 1961 (108 A).

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A NEW FORM OF CRANE-HOIST CONTROL USING A 3 : 1 POLE-CHANGING INDUCTION MOTOR

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SUMMARY

The basic practical requirements of crane-hoist drives are summarized and the latest developments, including closed-loop control methods, in satisfying such requirements with a.c. drives are discussed. In particular, the paper investigates the suitability of an economical design of a 3 : 1 pole-changing induction motor for crane-hoist drives.

In conjunction with a single-phase auto-transformer, the pole-changing motor enables the best use to be made of d.c. and a.c. dynamic braking, which further assists in reducing the energy dissipation in the motor circuits as well as reducing the number and size of the secondary-circuit resistors and contactors. It is shown that the performance characteristics are such as to satisfy crane-hoist requirements without undue complexity of the complete equipment.

(1) INTRODUCTION

During the first quarter of the century, the use of d.c. motors was regarded as essential in obtaining satisfactory operation of the hoist motion of cranes, the main objection to the use of a.c. induction motors being inadequate speed control for lowering loads. Nevertheless, the increasing availability of a.c. power supplies, coupled with lower maintenance and initial costs, eventually resulted in an extensive use of slip-ring induction motors employing secondary-resistance speed control on both the raising and lowering motions. In fact, to satisfy the desire for a rugged and reliable motor with still lower costs, squirrel-cage rotors of both the single-cage and double-cage types have been used for some light-duty cranes, with primary-resistance speed control.

In general, however, both primary- and secondary-resistance control of normal induction motors are considered unsatisfactory, and Higson[1] has stressed recently that the a.c. machine still compares unfavourably with the d.c. machine for crane duty, reiterating the inadequacy of the former on light loads. Not unexpectedly, considerable developments have now occurred in the method of speed control of the a.c. machine, which are additional to those discussed comprehensively by Broughton.[2] In order that such developments may be discussed satisfactorily, it is necessary to consider first the general requirements of crane-hoist drives.

(2) GENERAL REQUIREMENTS OF CRANE-HOIST DRIVES

Characteristics which are virtually ideal can be obtained with Ward Leonard control of a d.c. shunt-wound motor[3] and are shown in Fig. 1. Any number of characteristics intermediate to those given in Fig. 1 can be obtained by adjustment of a small resistor to satisfy almost all of the following basic practical requirements postulated by Sadler:[4] fast lowering speed for the light hook; fully variable speeds for all crane motions and permissible loads, with a 30 : 1 speed range not being too great; facilities for several constant speeds from zero to 30% full speed,

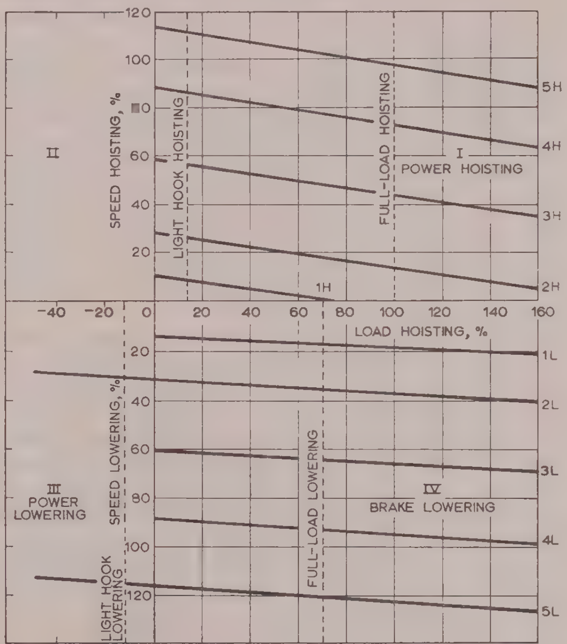

Fig. 1.—Torque/speed characteristics with Ward Leonard control.

or fully variable speed control in this range; rapid acceleration from 30% to full speed; continuous deceleration from full to creep speed when lowering; graduated braking for any traction motion, as distinct from hoisting and lowering; and preferably, a faster hoisting speed than normal full-load speed for raising loads under 30% of full load.

Obviously, these requirements can be satisfied at a considerable first cost of the Ward Leonard equipment, which is at present accepted for cranes handling loads greater than about 75 tons and which necessarily require a very sensitive and easily operated method of control. For smaller cranes, alternative a.c. drives exist or are being developed which often are acceptable technically and are much less expensive than a Ward Leonard equipment.

Again referring to Fig. 1, although quadrants 1 and 4 of the torque/speed co-ordinate system are of major interest, crane control necessitates a consideration of quadrant 3 also, since it represents 'power lowering' of the load, i.e. the motor drives in the lowering direction to overcome the frictional forces of the crane hoist to enable a non-overhauling load such as a light hook to be lowered, and provides greater lowering accelerations for overhauling loads. Quadrant 2 is of no practical interest, since the load is carried on a flexible cable or chain which prevents the motor exerting a 'driving-down' torque against any previously acquired speed of the load in the upward direction.

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It should be noted here that it is normal practice to take the full-load lowering torque in quadrant 4 as about 70% of the full-load hoisting torque in quadrant 1, the difference being due to friction of the hoist drive. Also, it is usual to assume that the driving-down torque in quadrant 3 required to overcome the friction of the crane, whilst lowering the empty hook, is about 12% of the full-load hoisting torque in quadrant 1.

(3) RECENT DEVELOPMENTS IN A.C. CRANE-HOIST DRIVES

Before discussing the latest developments, it is of interest to refer briefly to some of the control methods which are widely used at the present time.

An electrically controlled mechanical braking system,[5] operating against the driving motor during both lowering and raising of the load, provides improved control at the lower speeds and has found extensive application. Various d.c. and a.c. electrical methods of braking have been developed with the object of minimizing the wear and maintenance of the mechanical brake, by limiting its use to holding the load at standstill and for emergency stopping. Of these methods, d.c. dynamic braking[6] is the most widely used, despite the fact that additional secondary-circuit resistors and contactors are normally required at the higher braking speeds.

Much attention has been given to a.c. dynamic braking methods which avoid completely the need for a d.c. supply, such as the use of asymmetrical primary connections[7, 8] or asymmetrical terminal voltages[9] of the motor. Of these, the most widely used appears to be the simple method discussed by Schmitz[10] and others[11] in which only one of the motor terminals is connected directly to a supply line, the two other terminals being connected to their corresponding supply lines via a single-phase auto-transformer. However, overheating of the motor tends to limit the full-load landing speed to about 50%, which may be reduced to 18% by introducing capacitors into the secondary circuit.

Although first introduced into crane-hoist control by Wickerham[9] more than fifteen years ago, one of the latest developments in a.c. cranes is the more general use of the closed-loop principle, in which a signal dependent upon the speed of the load is compared with a reference or control signal and the difference fed back to the drive to maintain the speed within close limits of that corresponding to the reference signal, irrespective of the magnitude of the load or of extraneous disturbances such as voltage changes in the a.c. supply system. The most sophisticated application of this principle is afforded by control schemes employing completely static components, which eliminate reversing switches or contactors in the main supply circuit to provide automatic torque reversal, thereby allowing the static friction of a non-overhauling load to be broken by driving-down of the motor followed immediately by any necessary raising torque to maintain the lowering speed at a virtually constant value. Evidently, the latter schemes can also be applied to obtain extremely accurate position control of a suspended load, if required, since any tendency to overshoot the desired position can be automatically countered by a reversal of torque.

The essential differences in various control methods employing the closed-loop principle are in the arrangements for providing the speed signal and in the manner in which the net torque of the drive is controlled by the difference signal. The speed signal may be obtained indirectly from: a resistive drop in the secondary circuit of a slip-ring motor;[9, 12] an equivalent resistive drop in the primary circuit of a motor, on the assumptions that the magnetizing current is negligible compared with the load current of the motor and that the impedance of the motor referred to the secondary circuit may be taken as the ratio of the secondary resistance to the fractional slip;[13] and a bridge circuit having resistors as two of its branches, a fixed inductor as the third branch and one of the stator phase windings as the fourth branch.[14] Alternatively, and more commonly, the speed signal is obtained directly from a tachometer generator, coupled to either the motor or the crane-rope barrel.[15–19]

The net torque of the squirrel-cage driving motor, for a given speed setting corresponding to a given reference signal, may be controlled by the difference signal operating an eddy-current slip coupling between the motor and the crane gearing; a slip of zero to reverse full speed is obtained with an overhauling load, and in the case of a non-overhauling load the drive is transferred to a small auxiliary squirrel-cage motor and eddy-current coupling, incorporated within one casing, to provide the necessary driving-down torque.[19] Alternatively, the difference signal may operate on one or more saturable reactors inserted in the primary supply lines to vary the voltage, and thereby the torque of the motor.[9, 12–16] A schematic of each alternative is shown in Fig. 2, it being borne in mind that the latter alternative usually employs a slip-ring motor with secondary-circuit external impedances.

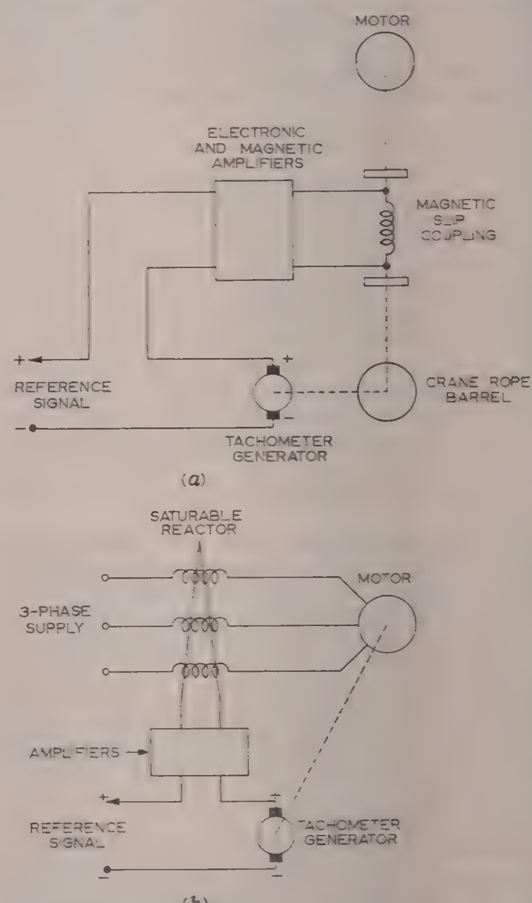

Fig. 2.—Schematic of closed-loop control.

(a) Eddy-current coupling method.
(b) Saturable-reactor method.

A partial schematic of another alternative,[17] which is particularly ingenious, is shown in Fig. 3. The arrangement uses two normal-type saturable reactors, SX_1 and SX_2, and a third

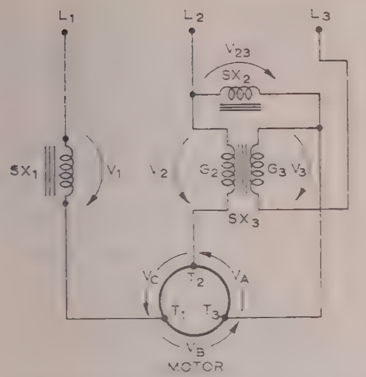

Fig. 3.—Partial schematic of saturable-reactor reversible control.

saturable reactor, SX_3, having two identical load or gate windings G_2 and G_3 on a common magnetic core, the d.c. control windings being omitted for the sake of clarity. Evidently, the reactive voltage drops V_2 and V_3 must be equal since they arise from gate windings embracing the same magnetic core, and are in phase with each other in the local circuit L_2, G_2, T_2, T_3, G_3, L_3. From Fig. 3 and the voltage vector diagram of Fig. 4(*a*), it

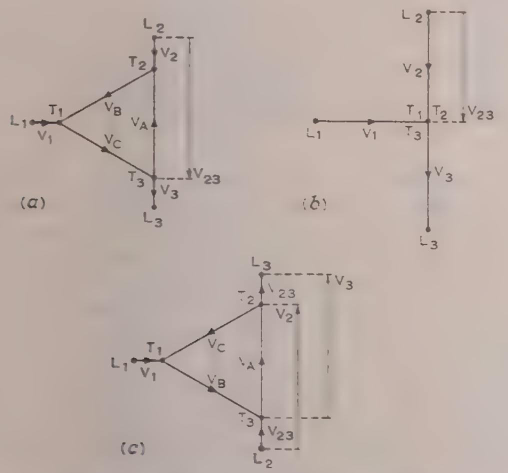

Fig. 4.—Vector diagrams of saturable-reactor voltages V_1, V_2, V_3 and V_{23} and motor phase voltages V_A, V_B and V_C.

(*a*) Maximum terminal voltage of motor for forward rotation.
(*b*) Zero terminal voltage of motor.
(*c*) Maximum terminal voltage of motor for reverse rotation.

will be seen that full-speed raising of the load is obtained with the motor terminals T_1, T_2 and T_3 virtually directly connected to the supply lines L_1, L_2 and L_3, respectively, when both SX_1 and SX_3 are 'fully on' (i.e. minimum reactance of the gate windings corresponding to maximum net d.c. excitation of their cores) and SX_2 is 'off' (i.e. maximum reactance of its gate winding corresponding to zero net d.c. excitation of its core), i.e. the reactive drops V_1, V_2 and V_3 are at their minimum voltages, the reactive drop V_{23} is at its maximum and the motor phase voltages V_A, V_B and V_C are at their maximum for forward rotation of the motor.

If now the net d.c. excitations of SX_1 and SX_3 are reduced whilst increasing that of SX_2, the voltage drops V_1, V_2 and V_3 increase until the motor has virtually zero terminal voltage (and torque), as shown in Fig. 4(*b*). If the net d.c. excitation of SX_1 is then increased back to 'fully on', whilst still further reducing that of SX_3 to 'off' and simultaneously still further increasing that of SX_2 to 'fully on', a minimum value of V_1, with simultaneously equal and maximum values of V_2 and V_3, are obtained such that the motor terminals T_1 and T_3 are virtually directly connected to lines L_1 and L_2, respectively, whilst the terminal T_2 is virtually at the same potential as line L_3 to give reversed terminal voltage (and torque), as shown in Fig. 4(*c*). It should be noted, in connection with the latter statement, that T_2 is virtually at the same potential as L_3 because V_2 and V_3 cancel each other in the local circuit T_2, G_2, SX_2, G_3, L_3.

Thus, with the saturable-reactor arrangement of Fig. 3, both the motor speed and torque may be varied in magnitude and direction by variation of the net d.c. excitation of the reactors, corresponding to variation of the reference signal in the closed-loop circuit, without the use of reversing switches or contactors and with very little terminal-voltage asymmetry of the motor.

Further alternative arrangements of saturable reactors, with or without transformers, have been given by Zollinger[18] and Leonhard[20] which introduce some degree of terminal-voltage asymmetry, in general, whilst providing static reversible control of the speed and torque of the motor. Such arrangements have been successfully employed on motors of greater than 100 h.p. rating.

Other very recent developments have also occurred which still adhere to the open-loop principle, e.g. the provision on the common magnetic circuit of a single motor of two electromagnetically separate stator windings, each of which reacts exclusively with only one of two electromagnetically separate rotor windings, to permit independent and simultaneous a.c. motoring or plugging and d.c. dynamic braking torques.[21] The special single machine of the latter control method is avoided in a still more recent development which utilizes two direct-coupled standard a.c. machines for providing simultaneous torques, as well as permitting both machines to be connected as a.c. motors.[22]

In general, all the above recently developed methods produce, or approach very closely to producing, the ideal characteristics of Fig. 1. Insufficient space (and information) is available to provide a fully detailed comparison of the methods with regard to their relative technical and economic merits, but it should be borne in mind that additional initial expenditure can result in reduced load-handling costs consequent upon an improved technical performance, as stressed by Sadler.[4] Nevertheless, extensive application exists for the generally less expensive open-loop methods, and the particular new method now to be considered comes into this category.

(4) GENERAL DESCRIPTION OF NEW METHOD

The new method employs a 3 : 1 pole-changing slip-ring induction motor with rotor resistance control of the speed. The main advantages of using such a motor are that creep speeds for both the raising and lowering of all loads are obtained readily, and regenerative braking is obtained at almost all higher speeds for the lowering of overhauling loads.

The line diagram of the control circuit is given in Fig. 5, and the contactor-sequence scheme is shown in Table 1. It will be seen that the top speeds are obtained with the motor connected for 3-phase operation from the supply system, and that lower speeds are obtained with a single-phase auto-transformer included in the connections of the supply system to the motor such that the latter is fed by a 2-phase system. In addition to providing 3-phase/2-phase conversion, the auto-transformer functions as a step-down transformer in obtaining d.c. dynamic braking control at creep lowering speeds, and as a means of obtaining asymmetrical voltage control at a creep hoisting speed.

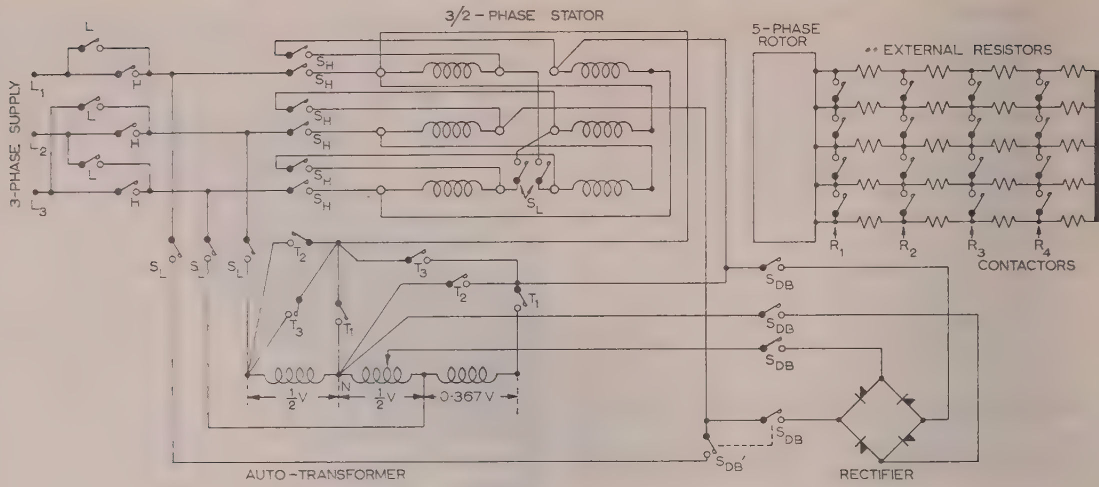

Fig. 5.—Schematic of equipment.

Table 1

SWITCHING SCHEME

Hoisting notches	Contactors								
	H 3-pole	S_L 5-pole	S_H 6-pole	T_1 2-pole	T_2 2-pole	R_1 4-pole	R_2 4-pole	R_3 4-pole	R_4 4-pole
1H	×	×			×				×
2H	×	×		×					×
3H	×	×		×				×	
4H	×	×		×			×		
5H	×		×					×	
6H	×		×				×		
7H	×		×			×			

Lowering notches	Contactors											
	L 3-pole	S_L 5-pole	S_H 6-pole	T_1 2-pole	T_2 2-pole	T_3 2-pole	S_{DB} 4-pole	S_{DB}' 1-pole	R_1 4-pole	R_2 4-pole	R_3 4-pole	R_4 4-pole
7L	×		×					×			×	
6L	×		×					×		×		
5L	×	×		×				×				
4L	×	×		×				×			×	
3L	×	×				×		×			×	
2L	×	×					×					
1L	×	×					×				×	

The rating of the single-phase auto-transformer is only about one-third of the motor rating.

The d.c. dynamic braking contactors S_{DB} are interlocked with the contactor S_{DB}' such that one is always at 'open' whilst the other is at 'closed'. This is necessary in order to isolate the a.c. supply line L_1 from the bridge rectifier whilst the latter is supplied from the lines L_2 and L_3 via the auto-transformer during d.c. dynamic braking operation. The number of contactors is not as large as might be expected, considering that pole-changing, d.c. dynamic braking and terminal-voltage asymmetry are used to permit a varied choice of performance characteristics. The particular choice corresponding to the auto-transformer and rotor-resistance tapping points selected for Fig. 5 will be discussed after a brief reference to salient features of the pole-changing machine.

(5) EXPERIMENTAL MACHINE

The available 3 : 1 pole-changing machine was a 400-volt 7·5 h.p. 12/4-pole slip-ring motor. In actual crane-hoist drives an 18/6-pole motor would be used, in general, from energy considerations and the fact that mechanical problems are likely to arise from gear wear and dynamic balancing of brake wheels at a 4-pole top speed. It was considered, however, that the per unit or per cent performance characteristics of the available machine would be representative of actual practice.

The machine is essentially of the same standard form as a non-pole-changing machine, since it has only one winding on the stator and only one on the rotor, and uses a standard frame size of a non-pole-changing motor. The 48-slot stator is that of machine No. 2 described in detail by Barton, Butler and Sterling.[23] The 60-slot rotor of the machine carries a 5-phase 36°-spread 4-pole winding which functions also as a 5-phase 108°-spread 12-pole winding without any change in its connections.

The connections of the stator half-phases for 3-phase p-pole and 2-phase $3p$-pole operation are shown separately in Fig. 6

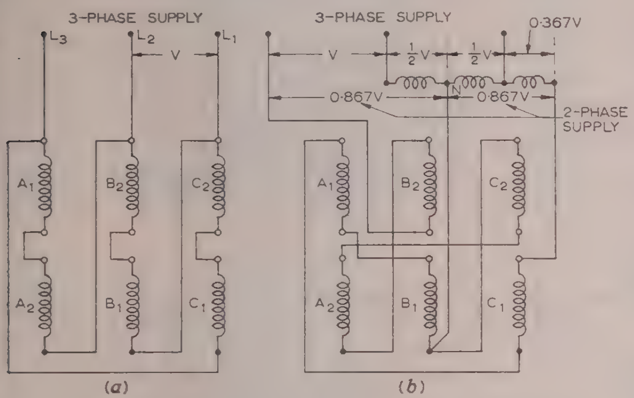

Fig. 6.—Pole-changing connections of stator winding.
(*a*) 3-phase p-poles.
(*b*) 2-phase $3p$-poles.
○ Terminals.

for simplicity. The particular sequence of connections of the half-phases in the 3-phase p-pole connection is adopted to minimize the number of contactors required in converting it to the 2-phase $3p$-pole connection. The $3p$-pole field arises because the zero-sequence connection of the three half-phases C_1, A_1 and B_1 in Fig. 6(*b*) (and similarly $-B_2$, $-A_2$ and $-C_2$) eliminates the fundamental and all harmonic fields except those of the triple harmonics, and of the latter, all except the third harmonic are negligible. Since the two half-phases of any one phase have a fundamental electrical displacement in space of 30°, they have a third-harmonic electrical displacement in space of 90°, and the three corresponding half-phases C_1, A_1 and B_1 (and similarly $-B_2$, $-A_2$ and $-C_2$) have a third-harmonic electrical displacement of 360° and are thus all in phase in space. Hence, each set of three corresponding half-phases may be connected in series as shown (or in parallel) to provide a balanced 2-phase $3p$-pole winding.

Five slip rings are necessary for connection of the rotor phases to five external resistor branches. Obviously the total rating of all five external resistors is the same as that required by a 3-phase 4-pole or 3-phase 12-pole rotor winding. It is equally obvious that the external resistors of the 5-phase rotor are selected such that, as far as possible, the same resistors are used for both 4-pole and 12-pole operation of the machine.

The equivalent circuit of the machine for the alternative pole numbers, which have been obtained experimentally as described by Morris,[24] is shown in Fig. 7.

(6) PERFORMANCE CHARACTERISTICS

The torque/speed characteristics of the experimental machine are shown in Fig. 8. All the characteristics have been obtained experimentally except the first two lowering characteristics, 1L

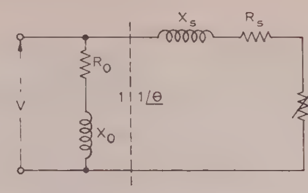

Fig. 7.—Equivalent circuits of 12 : 4 pole-changing induction motor with 5-phase wound rotor.

	R_0	X_0	θ	X_s	R_s	R_2
4-pole motor ..	ohms 23	ohms 245	deg 0·5	ohms 17	ohms 2·86	ohms 3·25
12-pole motor	7·7	56	4	27	5·64	8

and 2L, corresponding to d.c. dynamic braking, which have been calculated in accordance with the theory given by Butler.[25] The hoisting characteristics 1H–4H and the lowering characteristics 1L–5L have been obtained with the 12-pole connection of the motor, and the remaining characteristics with the 4-pole connection.

The characteristic 1H, corresponding to the first notch of the controller, shows that, during hoisting, the creep speed varies from 17% to zero for loads varying, respectively, from a light hook to 70% full load. Evidently, the motor fails to hoist a load greater than 70% full load, but the important practical point here is that the hoist drive cannot be overhauled in the lowering direction on this notch and it therefore provides safe handling of all loads at all times. The characteristic is obtained by applying the full voltage V of the 3-phase supply to one phase of the 2-phase connected motor and $\frac{1}{2}V$ to the other phase via the auto-transformer, with the rotor resistance such that maximum torque would be obtained at a slip of about 2 for balanced 2-phase voltage operation. The actual unbalanced voltages of V and $\frac{1}{2}V$ are equivalent to positive-sequence and negative-sequence voltages of, respectively, 83·5% and 35·6% of the rated voltage, $\sqrt{3}V/2$, of the motor.

Characteristics 1H and 2H are obtained with the same rotor resistance, but the latter is obtained with balanced 2-phase operation of the motor at its rated voltage of $\sqrt{3}V/2$. A creep hoisting speed of about 5% full speed is obtained at full load on notch 2H. Fine control above this speed, up to 25% at full load, is obtained by the succeeding notches 3H and 4H on the 12-pole connection of the motor.

In line with accepted good practice, the load is then rapidly accelerated to full speed on the remaining hoist notches 5H, 6H and 7H using the 4-pole connection of the motor, as previously mentioned. Thus, all hoisting characteristics except 1H are obtained with balanced voltage operation of the motor.

During lowering, d.c. dynamic braking is used to obtain the creep speeds corresponding to characteristic 1L and the low speeds of characteristic 2L. A direct current of about 140% rated value (r.m.s.) is supplied to both phases, with the rotor resistances set such that, even on notch 1L, a braking torque of 100% full load is available at 130% full speed. Hence, stable lowering of the load is obtained even when braking is applied on either of notches 2L and 1L at the maximum permissible speed.

Single-phase dynamic braking is used to obtain characteristic 3L, i.e. a voltage V is applied to one of the phases whilst the remaining phase is short-circuited. The power lowering is not available on notches 1L, 2L and 3L; they are restricted to over-

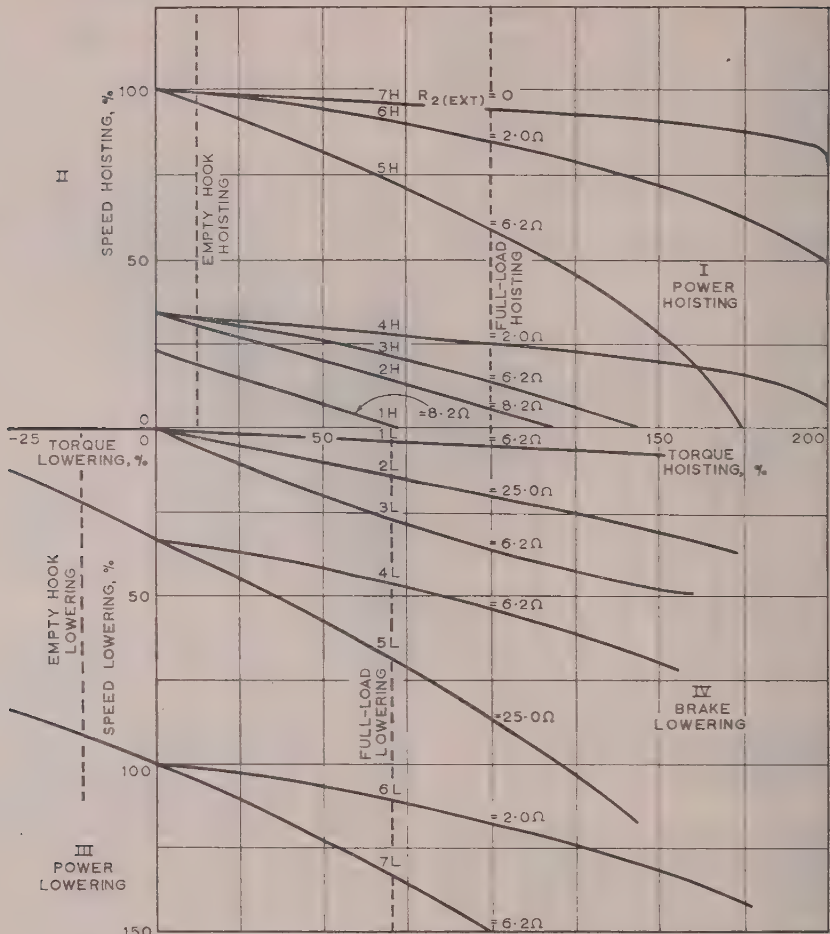

Fig. 8.—Torque/speed characteristics with 3 : 1 pole-changing motor.

hauling loads, but with such loads, finely controlled speeds are obtained down to a creep speed of about 4% full speed. Power lowering becomes available on notches 4L and 5L, with lowering speeds of 33% for a load which just fails to overhaul the crane and about 25% for an empty hook, regenerative braking being obtained for overhauling loads. In the latter case, the lowering speed is controlled at about 70% and 47% for full load, which falls to 33% for loads which just overhaul the crane.

Regenerative braking is again obtained on notches 6L and 7L, by transferring from the 12-pole to the 4-pole connection. Notches 6L and 7L provide the maximum power-lowering speeds of non-overhauling loads, being 100% for a light load to about 95% for an empty hook. A full-load lowering speed of about 130% is obtained on notch 7L, which is considered to be the maximum permissible in general crane practice. It is also general practice to provide an overspeed switch, which would operate for the present system if too high a rotor resistance were inserted accidentally, 200% full speed being possible at full-load lowering if all the available rotor resistance were inserted accidentally.

(7) DISCUSSION

The currents obtained, during both lowering and hoisting for the new method of control, are such that considerably less heating of the motor primary and secondary circuits occurs than is usual in crane practice. This result is of particular significance in the case of totally enclosed motors, and is not without some advantage for other types of enclosure, despite the fact that the limiting factor then tends to be the peak torque rather than the temperature rise. In either case, the reduced heating results in a reduction in the size and weight of the secondary resistors, which is of some considerable importance. Currents of the order of 130% rated value are obtained on notches 1H, 1L, 2L and 3L, but do not exceed 100% on the remaining hoisting characteristics and 85% on the remaining lowering characteristics. This compares very favourably with the values of 200–300% rated current obtained on some notches of a number of alternative methods.

In connection with the size of the secondary resistors, it should be noted that the use of d.c. instead of a.c. dynamic braking to obtain the characteristic 3L, with a full-load speed of about 25%, would have required the rotor resistance to be increased about eight times. This would have necessitated additional secondary-circuit contactors, as well as increasing by 100% the size of the total bank of resistors.

The connections for the motor during lowering on notches 4L–7L are unconventional in that they are always such as to drive down the hook, although regenerative braking is obtained above a given speed for a given overhauling load, and steady-state driving-down is obtained only with non-overhauling loads.

hus stable lowering at super-synchronous and sub-synchronous peeds is obtained without the need for reversal of the motor onnections. In consequence, with the values of rotor resistance mployed, the current inrush at switch-over to power lowering sufficiently low for protective measures, such as including esistors in the primary lines, to be unnecessary.

The torque/speed characteristics approach closely those orresponding to the ideal performance and, in particular, ermit load positioning. In the latter connection, Zollinger[18] tates, in describing his static reversible drive, that the first oist characteristic is adjustable during installation from 30 to 0% at no load, but from experience the curve having a no-load peed of about 40% is the most used. Obviously, from Fig. 8, he present new method can satisfy this and much more onerous onditions of operation, at a great saving in expense and weight f equipment. The availability of the single-phase auto-ransformer and pole-changing motor enables other asym-etrical voltage characteristics to be obtained for little extra eating of the motor and expense of the equipment, which still ore nearly approach the ideal performance of a crane-hoist rive.

(8) CONCLUSIONS

It has been demonstrated that an economical form of 3 : 1 ole-changing induction motor, in conjunction with a single-hase auto-transformer, can provide performance characteristics which satisfy the requirements of many crane hoists without ndue cost and complexity of equipment. In this practical pplication, as in all other continually reversing drives, the ole-changing motor provides a considerable reduction of the nergy dissipated in the primary and secondary circuits of the otor. Thus, the use of a totally enclosed motor presents no erious thermal-stress problem.

The availability of the auto-transformer permits the best use o be made of both d.c. and a.c. dynamic braking, with a sub-tantial reduction in the number of resistors and contactors equired in the secondary circuit of the motor; this advantage is aterially enhanced by the use of the pole-changing motor. In ddition, the reduced energy dissipation of the control method esults in a substantial reduction of the size and weight of the ecessary resistors.

There is little doubt that the degree of automatic control vailable with some of the latest methods, employing the closed-oop principle, provides advantages in load handling which fully warrant their adoption in many cases, and these methods are kely to compete seriously even with Ward Leonard equipment or the larger cranes. However, a wide field remains in which pen-loop methods, more effective than hitherto, can be justi-ably employed. The latter category includes the 3 : 1 pole-hanging method.

(9) ACKNOWLEDGMENTS

The facilities afforded by the University of Sheffield, the rovision of the pole-changing motor by the Lancashire Dynamo nd Crypto Co., Ltd., and helpful discussions with Mr. H. terling are gladly acknowledged.

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[The discussion on the above paper will be found overleaf.]

DISCUSSION BEFORE THE UTILIZATION SECTION, 12TH JANUARY, 1961, AND THE NORTH MIDLAND CENTRE AT LEEDS, 1ST NOVEMBER, 1960

Mr. G. V. Sadler: The authors refer to Fig. 1 as a typical Ward Leonard control diagram. The essence of crane control is to flatten out the curves for each of the notches provided. This particular diagram is not quite up to date, because with the latest types of Ward Leonard control, particularly with closed-loop control, these notch lines are much flatter, and there is only a difference of $1\frac{1}{2}\%$ between light-load and full-load hoisting and lowering. Most of the closed-loop systems today can produce horizontal straight lines for every notch. That is an ideal which has only been achieved recently, and as the authors say, they were excited by the use of the closed-loop control. I would agree, because I think that its introduction has been one of the greatest advances in crane control. In crane operation a steady creep speed at all loads is an advantage which has to be used to be appreciated. If the user of the crane can be sure that at a particular notch the hook will creep up and down at 3 or 4% of the full-load speed, he gets a much greater sense of safety. This problem of inching is one of the most difficult a crane maker is up against. Inching a load, particularly a heavy one, does not do the crane any good. It imposes unnecessary stresses on the gears, the structures and the ropes, to say nothing of the electrical equipment.

There is a tendency today to increase crane speeds, particularly full-load hoisting speeds, and if these are increased, the ratio of full speed to creep speed should be increased also. Many makers in the past have felt satisfied if they give an a.c. creep speed of 10%, but that does not give a very low speed, particularly if the full speed is, say, 40 ft/min. Therefore, I am glad that the authors in their characteristic curve give a figure which is nearer 4%.

Is the pull-out torque at low speed on the pole-changing machine sufficiently adequate to cater for the condition that the speed of lowering a full load can be reduced quickly from notch 7L to 1L, without any trouble occurring? Also is the torque provided on 1L sufficient to pull up a fairly light load when full-speed hoisting? On 1L notch, the over-winding switch is not operative, and therefore it is no use relying on the overwinding limit switch if the driver has switched over into 1L inadvertently.

The economic side of this control is very important. A rough estimate of the cost of this scheme using a motor of about 25 h.p. is approximately 70% of the cost of a closed-loop eddy-current coupling scheme. The cost of the authors' scheme is about four times that of standard control gear. That of the Ward Leonard scheme can be up to five and a half, to six times. Those of many systems vary between three and a half and four times that of the straight control gear, so that this form of control, if adopted, is by no means out of the price range. The motor, however, is not a standard machine, and some crane users might object to having to buy two of these when they wish to have a spare motor. Have the authors considered using two standard motors, rather than using this one motor with a 5-phase rotor?

I am glad to know that the authors realize the practical point that hoist drive cannot be overhauled in the lowering direction; however, the slope of curve 1H in Fig. 8 should be flatter, because I am sure that 1H and 1L are the two most important curves in the whole paper.

Mr. E. R. Radway: My interest is in high-speed 3 to 10-ton-capacity level luffing cranes found in dockside practice. Such cranes are now supplied by alternating current, and the designer's attempts to obtain the d.c. characteristics for the hoisting motion have led to many complications. Accepting that Ward Leonard control can give ideal characteristics, the authors' statemen restricting this control to loads of about 75 tons, or over, is no realistic. Operating speed, as well as load, should be th consideration.

To be adapted in dockside practice, the authors' scheme mus compare favourably in capital and maintenance costs, as wel as operating characteristics, with Ward Leonard controls. I high-speed cranes the hoist-drive inertia should be kept as low as possible. How does the inertia of the proposed 3 : 1 pole changing motor compare on an equal horsepower basis with a standard form of machine?

The low currents of the proposed scheme are a very goo feature, but the torque/speed characteristics of Fig. 8 are no ideal. For cargo handling practice 1H should be flatter, e.g commencing at 10% of light hook speed, and attaining full loa before stalling. If that could be attained, notching 2H, 3H an 6H could be omitted. Whilst the lowering characteristics loo satisfactory, recent experience of d.c. injection braking an power lowering on cargo cranes raises doubts regarding thei suitability. The crane operators prefer a positive movemen on each lowering step, and not reliance upon an overhaulin load. 'Pull through' has been experienced, i.e. the load ha speeded up above the power-lowering speed during the perio that elapses between removal of power lowering and establish ment of d.c. injection braking. This is most disturbing to th operator. Can it occur in the proposed scheme?

Consider the case of the load failing to overhaul the crane The operator, finding no movement, goes through notches 4L 5L, 6L and 7L to give full-speed lowering, and as the leve approaches at which he wishes to land the load, returns the con troller to notches 'off', 1L, 2L or 3L, stopping the load. T lower again, he must power lower by returning to notch 4L, an that immediately lowers at 33% full speed. This does not giv effective control, especially as the load may be out of the sigh of the operator in the hold of a ship. Would the author comment on these possible weaknesses?

Mr. R. A. West: Contrary to the authors' statement, quadran 2 can be of interest when it is necessary to absorb the energy o rotation, i.e. to stop quickly after hoisting with empty hook This is important for large d.c. motors.

In Fig. 8 the torque for lowering the empty hook should b less than for hoisting it. The latter varies between 6 and 12%

Counter-current braking is not complicated, as suggested but simple, although wasteful of power. It is true that asym metrical stator control causes overheating, but this type is no frequently used, as might be inferred from Section 7. Plai reversing is popular, and this is combined with counter-curren lowering on the heavier duties: these two systems account fo at least 90% of a.c. hoists. With the authors' scheme it i necessary to reconnect the motor whilst running. I do no consider this safer than the reversing needed for counter-curren braking, but the authors have claimed an advantage in thi connection. Fig. 5 shows 44 contacts compared with 18 fo counter-current braking. The battle of the terminals is definitel on; it is vital for the large number of cranes where the hoistin winch moves with respect to the driver and control gear, as i the case with most overhead travelling cranes. The addi tional collectors may be more costly than the extra feature for the motor and control. For this reason feedbacks ar unpopular.

The crane is not a very efficient form of handling machiner Hence, the efficiency of the hoist is usually of less consequen than simplicity, reliability, or first cost. Nonetheless th

uthors' proposal is an extremely interesting development from ne recent series of papers on modified induction motors.

Table 1 would be improved by showing $S_{DB'}$ closed on all oist notches. Fig. 6(*b*) would be clearer if L_1, L_2 and L_3 were nown at the top in reverse order compared with Fig. 6(*a*). rimary resistance control is mentioned in connection with ther schemes, but is not good practice without expensive autonatic control, as it is liable to throttle the pull-out torque until ne load drops. A pull-out torque of 2·5 p.u. is commonly ecommended since:

$$\text{ull-out torque} = \frac{(\text{Test load}) \times (\text{Starting effort})}{(\text{Minimum voltage})^2}$$

$$= \frac{1{\cdot}25 \times 1{\cdot}5}{(0{\cdot}9)^2} = 2{\cdot}3$$

In Fig. 8, 2·0 p.u. pull-out torque is inferred. This means ne motor is rated for this duty at $1\frac{1}{2}/4\frac{1}{2}$ h.p. as against the connuous rating of 3/9 h.p. given in Reference 23. More informaon on this aspect should be given because of the well-known ifficulty of obtaining adequate pull-out torque with low-speed c. motors.

Mr. John Baker: With speed control there are a number of ctors to be considered. Many crane users merely want one w-speed step for positioning and others want a full range of eeds. As a crane maker I find it does not work this way in ractice. One buyer will not order cranes fitted with speed ontrol and another has speed control whether he needs it r not.

Cranes are getting bigger in lifting capacity and the user is emanding higher speeds, so that for all large cranes today me form of speed control is essential. As we are dealing with rge motors, reasonable efficiency is also expected. My xperience of a.c. speed control is that the safeguards necessary prevent it lowering when it is not meant to lower and to make hoist when it is meant to hoist make the equipment very pensive, and I am certain that some of the a.c. schemes which ave been tried are more expensive than the Ward Leonard.

I appreciate the authors' efforts to dispense with contactors d resistors. It is not only a question of cost but of where to t all the resistors and contactors. In my experience the polenanging motor, if it gives the torque characteristic required, is bulky machine, and in some cases it is difficult to accommodate the crane.

I think it is important to make a crane that will not allow the ad to lower on first notch hoist.

With regard to the question of braking, as cranes get bigger d speeds increase the ordinary type of friction brake is no nger capable of standing up to the wear and heat on hightput cranes: it has become essential to slow the load up ectrically and use the brake only for holding. That is another ason why we must be able to go from full speed to the off sition in complete safety and not blow the contactors out.

In disagreement with Mr. Sadler regarding Fig. 1, I want to ve the steepest curve I can for full-power notches.

Mr. A. E. Williams: A lot of work has been done on a.c. ntrol schemes and there are a large number of completely fferent ones in existence. This makes it increasingly difficult think of a new one and also proves that no one scheme is mpletely satisfactory for all applications. Many cranes nnot justify the expense of Ward Leonard control, and a uch simpler control is more suitable. The present scheme is o complicated to cover this field, but it is possible that a much npler scheme can be evolved based on some of the principles umerated in the paper.

Mr. H. Sterling (*at Leeds*): Whenever pole-changing motors are used and single- and dual-wound arrangements are available, it is necessary to compare the overall cost of the two alternatives. Although I have no exact costs available, it appears that in this instance the machine with a dual-wound stator and a single-winding 5-phase rotor might prove more attractive than the single-winding stator machine proposed by the authors.

The two-winding stator machine need not have a higher moment of inertia than the single-winding alternative because the extra winding space can be provided by increasing the outside diameter of the stator laminations, while keeping the stator bore constant. The cost of the dual-wound motor may be about 40% greater and its outside diameter about 15% larger, but this is balanced by the following advantages: 6 stator control leads, instead of 9; 7 stator contactors (6 if curve IH is omitted) instead of 9 or 11; smaller auxiliary transformer, which is only needed for d.c. injection braking; facility to reduce the low-speed leakage reactance and increase the low-speed pull-out torque by placing the low-speed stator winding at the mouth of the slot (this is particularly important when 6/18 pole and 8/24 pole machines are considered).

The pole-changing hoist motor, whether with single- or dual-wound stator, will have considerably lower losses during running, acceleration and braking, than any of the other a.c. control schemes which are described in the paper. The losses may even be comparable with those for Ward Leonard control, because there are no motor-generator no-load losses when the crane hoist is not working.

Dr. O. I. Butler (*in reply*): It is correct, as stated by Mr. Sadler, that characteristics can be obtained which are more nearly horizontal than those shown in Fig. 1. However, in general, the cost of the closed-loop control elements is increased in obtaining such characteristics, whilst retaining stability of operation under all possible transient conditions. Calculations have shown that no trouble would occur in passing quickly from notch 7L to 1L, or in switching inadvertently to notch 1L with a fairly light load moving at full-speed hoisting. Mr. Sadler's cost calculations are of interest, and it is most unlikely there is anything to be gained by using two standard motors in place of one motor with its 5-phase rotor.

The characteristic 1H of Fig. 8 cannot be modified readily to provide the 10% light-hook speed and full-load torque specified by Mr. Radway, but the inertia of the motor should not greatly exceed that of a standard slip-ring machine, and 'pull through' should not occur. It is appreciated that a lowering speed of 33% full speed is unlikely to provide the required degree of control when the load is out of the vision of the crane driver: however, the use of an asymmetrical supply, similar to that used to provide the characteristic 1H, can provide a further lowering characteristic intermediate to 2L and 4L which could quite well be satisfactory.

With regard to quadrant 2 of Fig. 1, although the paper is correct in stating that the flexible cable or chain prevents any driving-down torque being exerted on the load, Mr. West is equally correct in pointing out that the energy of rotation of the motor is quickly absorbed by the friction of the drive; i.e. the vertical broken line for light-hook lowering in quadrant 3 should be extended into quadrant 2, thereby making the latter quadrant of some interest. The fact that plain reversing and counter-current braking is used for at least 90% of a.c. hoists accounts, probably, for most of the criticism that the a.c. motor still compares unfavourably with the d.c. motor.

The increased bulk of a pole-changing motor, pointed out by Mr. Baker, is very significant when two separate stator windings and/or two separate rotor windings are employed. However, the pole-changing motor described in the paper has a single winding on both the stator and the rotor; in conse-

quence, it will not be much more bulky than a standard non-pole-changing motor rated for the higher speed.

The degree of complication depends upon the need for fine control under all operating conditions, and Mr. Williams is correct in suggesting that the scheme described in the paper can be readily simplified to satisfy less onerous demands on the control system.

The choice between a single-winding and two-winding stator, as discussed by Mr. Sterling, is probably most affected by the overall dimensions of the respective motors, bearing in mind that housing of the remainder of the electrical equipment should not provide any real difficulty.

The authors would like to express their appreciation of the many points raised by manufacturers and users of cranes, based on a wealth of experience in the field, which do not require further comment.

NEW CRANE HOIST CONTROL

By R. A. West, B.Sc.(Eng.), Associate Member.

(*Communication received* 23rd *January*, 1961.)

Grabbing cranes are used on a heavy duty-cycle to get loose raw material such as iron ore, coal, coke, bauxite and sand shifted as quickly as possible. Control of speed need not be refined, full speed, slow speed, and off being usually adequate. In addition, electrical braking, by use of motors when stopping after lowering, is essential to reduce the wear of the linings of the electro-mechanical brakes. The grab is provided with a self-contained mechanism by which it can be opened and closed by means of the relative movement between two sets of hoisting ropes. One set is termed the holding (or opening) line, the other set the closing line. As the amount of material which the full grab can contain is of about the same weight as the grab itself, it is common practice to provide a grabbing crane with two separate and identical hoisting winches each driven by a motor of a power equal to one-half of that required to hoist the full grab at full speed. These two motors are controlled by two similar contactor control equipments with separate master switches. These switches have adjacent handles so arranged that the driver can easily move both simultaneously by using only one hand.

Suppose the grab is deposited with its jaws open on the pile of loose material: the driver starts up the closing-line motor in the hoisting direction, and this motor runs at full speed for a few seconds. During this period the closing ropes exert a vast force through the internal mechanism of the grab to close the jaws which scoop up the material. Owing to the mechanical advantage of the mechanism and the shape of the jaws, the grab prefers to bite into the material rather than to lift clear away from it. If the grab digs downwards, the operator may need to pay out some holding rope to compensate for this by running the holding motor momentarily in the lowering direction. Under these conditions, of course, the two hoisting motors are momentarily running in opposite directions. As the jaws come together the load on the closing motor builds up to an overload. A calibrated stalling relay will then slow down this motor. In the case of a slip-ring motor this is accomplished by inserting rotor resistances.

A good driver will anticipate the action of the stalling relay by placing the master switch of the holding line in the full-speed hoisting position so that this motor attains full speed in time for both motors to pull the grab away with jaws shut and a full burden. In practice, with a.c. slip-ring motors, or d.c. series motors, or with a Ward Leonard system, there is no difficulty in getting the motors to share the load whether hoisting or lowering. So long as the pulls in the two sets of ropes are approximately equal, the grab will stay locked shut. In fact, the mechanical advantage of the closing ropes within the grab mechanism is so great that the pull on the holding ropes must greatly exceed that on the closing ropes before the jaws will open.

When the operator wishes to discharge the grab, he will place the closing-line master switch at full-speed lower, leaving the holding motor at rest. The grab mechanism is designed so that it will readily fall open and will thus take up the closing rope as the motor pays it out, keeping this rope under a reasonable tension and not allowing slack rope to gather on the closing barrel.

When the operator wishes to place the fully opened grab back on the pile to gather another load, an interesting problem arises. In order that the grab may stay wide open, its whole weight is supported on the holding line whilst lowering at full speed, and the closing line must pay out rope at an equal speed with virtually no load on the motor. There is but little difficulty in achieving this with d.c. series motors or with Ward Leonard. With standard slip-ring motors the problem is not electrically soluble. The holding motor is required to provide 0·80 p.u. hoisting (negative) torque and may be expected to run at 1·03 p.u. speed if all the rotor resistance is cut out, or at any convenient higher speed if the appropriate value of rotor resistance is inserted. The closing motor is required to run also at 1·03 p.u. speed, or higher, whilst exerting a drive down (positive) torque of 0·10 p.u. in order to pay out the rope: no help can be gained from the grab itself, as the least pull on the closing rope will cause the grab to close.

In practice the driver will, whilst lowering, reduce the speed of the holding line when necessary to allow the closing line to catch up. In some applications this is not inconvenient. In any event the maximum lowering speed for the empty grab is limited to synchronism, whilst for the full grab it may be 1·3–1·5 p.u., depending upon the capability of the motors. This is the reverse of the desired condition that the empty grab should be lowered at least as fast as, and preferably faster than, the fully loaded grab.

One mechanical solution of this problem which has been frequently used in the past is to change the gear ratio so that the closing line is 0·05–0·08 p.u. faster than the holding line. The drawbacks to this are twofold: (i) The maximum speed for lowering the empty grab is still only synchronous speed, and (ii) there must be a difference in diameter between the barrels of the two winches, alternatively a difference in the gear reduction

Mr. West is with Contactor Switchgear Ltd.

between the motors and the barrels, either of which is objectionable for ease of production and maintenance.

Dr. Rawcliffe's series of papers on pole-amplitude modulation provides another solution of this problem. The speed/torque curves in Fig. 1 show the application of a 10/14-pole closing-line

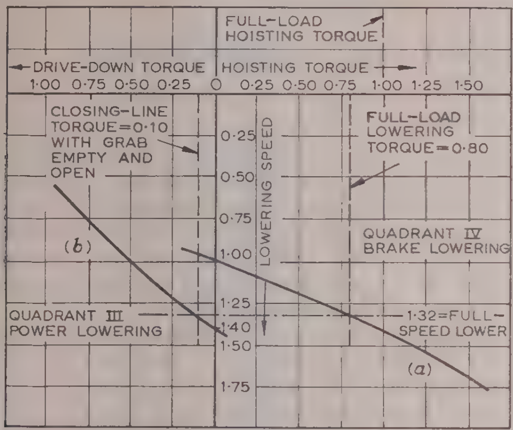

Fig. 1.—Speed/torque characteristics for 2-motor grab crane at full-speed lowering with empty open grab.

(a) Holding line supporting whole weight of grab.
(b) Closing line paying out rope at same speed, using a 10/14-pole closing-line motor and a 14-pole holding-line motor.

motor for this purpose, arranged so that the maximum speed for lowering the empty grab open is 1·32 p.u., which speed would be equal to or greater than the maximum speed at which the full grab would be lowered. It should be explained here that the usual practice is to provide an extra lower notch on the closing-line master switch (compared with the holding-line switch) which is only used when it is desired to lower the grab open at full speed. For the present application this would change the closing motor from 14 to 10 poles. When lowering the grab closed, either empty or fully loaded, the closing-line motor would operate on 14 poles, and both it and the holding line at full speed would share the load equally on coincidental curves (a). The empty grab represents half-load on each motor at 0·40 p.u. torque and 1·16 p.u. speed, whilst full grab can attain a maximum of 1·32 p.u. speed.

There are, of course, still some difficulties to be overcome. First, 14 poles produce too low a synchronous speed for economy, i.e. too large a frame size and/or too low a pull-out torque. Crane motors, especially on grabbing duty, should be capable of 2·5 p.u. pull-out torque, so that this design would be costly. If 8/10 pole-amplitude modulation were adopted, the maximum lowering speed with empty grab would be reduced to 1·18 p.u. Secondly, the number of contactors and terminals to produce modulation in a (wound) rotor would be costly. In the discussion on Butler and Ahmad's paper* it was emphasized that the battle of the terminals was very important. Bridge-type overhead travelling cranes, common in steelworks, usually have the hoisting machinery on a trolley which moves with respect to the driver and control gear. For these the additional terminals for modulation require an equal number of wires in the collector system which will comprise either T-bars and slippers or alternatively flexible cables with a draw-curtain suspension. Fortunately for the present application, by far the largest number of grabbing cranes are arranged with the operator and the control gear on board the structure which carries the hoisting machinery, without relative movement between them. This applies both to dockside jib cranes and boom unloaders, as well as to ore-bridges in steelworks.

It is important to realize that additional collector gear and wiring is likely to be more costly than the pole-changing motor and control gear. In order to reduce the cost, it might be possible to use a squirrel cage of high resistance and low rating for the high-speed operation of the closing motor. The power required is negligible. There must be an adequate drive-down torque when accelerating from rest to full speed lower, in order to keep up with the rapid acceleration of the holding motor which is aided by the weight of the grab. If this is not available by direct switching of the cage, an alternative, which is an easy control-gear requirement, would be to accelerate on 14 poles and change automatically to 10 poles when full 14-pole speed was reached, provided that the closing master switch was in the 10-pole notch.

* See page 222.

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MEASURED AND ELECTRICAL-MODEL CHARACTERISTICS OF BUILDINGS HEATED BY FLOOR THERMAL STORAGE

By E. D. TAYLOR, M.Sc., A.Inst.P., B. BERGER, B.Sc.(Eng.), Associate Members, and G. BLAYLOCK, B.Sc.

(*The paper was first received* 4*th April, and in revised form* 19*th October*, 1960.)

SUMMARY

The thermal design and behaviour of single- and multi-storey floor-heating installations based on measurements on an electrical analogue are described. The practical justification for the approach is the good agreement between the measured temperature and heat-flow data on a 3 MW floor-heating installation for a single-storey factory and the predictions of the analogue.

(1) INTRODUCTION

The ideas of comfort and the construction and performance of floor-heating installations have been adequately discussed in the literature.[1–5] The object of the present paper is to consider the basic principles of thermal storage, such as ground temperature distribution, heat flow, efficiency and time-constants, by comparing actual measurements on a 3 MW floor-heating installation for a single-storey factory with the predictions from a thermal analogue and thence to use this analogue in the thermal design of other structures.

The thermal analogue depends on the identity of form of the differential equations of thermal and electrical conduction and capacitance.[6–8] The exact equations of convection and radiation loss from the floor are approximated by a linear resistance, i.e. the assumption is made that Newton's law of cooling holds. Solar radiation reaching the floor is represented by a current input at the junction between this heat-transfer resistor and the first resistor of the floor model (Fig. 1). When changes in outside air temperature are simulated, the capacitors in the model must be connected to a reference line from which all

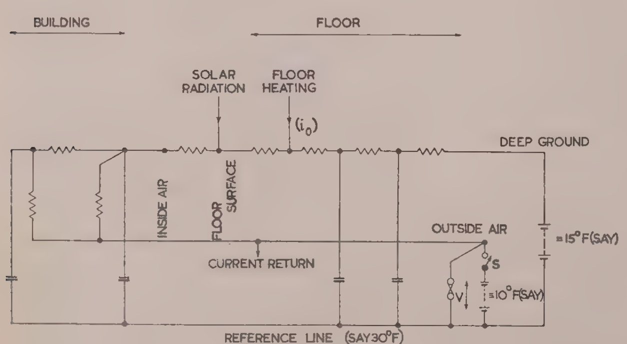


Fig. 1.—Basic electrical model.

voltages representing temperatures must be measured. In Fig. 1 the circuit arrangement for an abrupt change in outside air temperature is also given.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
The authors are with C. A. Parsons and Co. Ltd.

The electronic simulator was of conventional design, having resistance- and capacitance-network boards and constant-voltage and constant-current units controlled by a reference source giving either direct current or a 50 c/s square wave of 1 : 1 mark/space ratio. An oscilloscope and moving-film camera were found to be superior to a direct-writing recorder for making accurate transient measurements.

The factory floor and sub-soil were represented in the model (Fig. 2) by a 20 ft-deep column, of cross-section 1 ft^2, on the assumption that the horizontal heat flow beneath an average square foot of floor is negligible because of the large total area. The initial temperatures in the ground were approximated by a linear distribution from outside air level to a constant 45° F at 20 ft (as discussed in Section 3.1). The structure of the building was represented by apportioning the leakage resistances and thermal capacities of the various components to the average square foot of floor; the steelwork and internal atmosphere were assumed to have only thermal capacity while the ventilation was calculated as an additional leak to the outside atmosphere. The necessary properties of the flooring and constructional materials were taken principally from information in the 'Guide to Current Practice' of the Institution of Heating and Ventilating Engineers. The scale factors of the analogue were chosen so that 1 volt was equivalent to 0·4° F, 1 mA to $6\frac{2}{3}$ Btu/h and 1 sec to 50 days.

(2) MEASURED CHARACTERISTICS OF A SINGLE-STOREY FACTORY

(2.1) The Factory and its Instrumentation

This factory is heated solely, apart from industrial gains, by an off-peak electrical floor-heating installation of 3 MW capacity. The electricity consumed by the installation is separately metered and has a special tariff. Power may be used only between 6 p.m. and 7.30 a.m. and between 12 noon and 3 p.m. During these hours it is charged at the cheapest per-unit rate to the factory with no charge for maximum demand. The building is a single-storey steel-girder structure with a floor area of 250 000 ft^2, 82% of which is used for floor heating. The whole of the roof is double glazed, 30% of the wall is of $15\frac{1}{2}$ in-cavity-brick construction and the rest is aluminium with a $\frac{1}{2}$ in insulating board. The uses of the building include light and heavy machining, light and medium assembly, hydraulic testing, etc., with stores and offices for the production personnel. The large operations require the heavy structure necessary for overhead cranes which involves some 6000 tons of steel. The floor-heating elements themselves are of single-cored mineral-insulated cable giving a power input of 15 W/ft^2 at a spacing of 7 in, and are laid on a concrete bed and covered with screed and concrete tiles.

A section of the tiles, screed and sub-soil is shown on the right-hand side of Fig. 2, while the left-hand side shows their analogue equivalents and those of the remainder of the building. This Figure also indicates the probes installed in the building for measuring the ground temperatures. Three types of probes

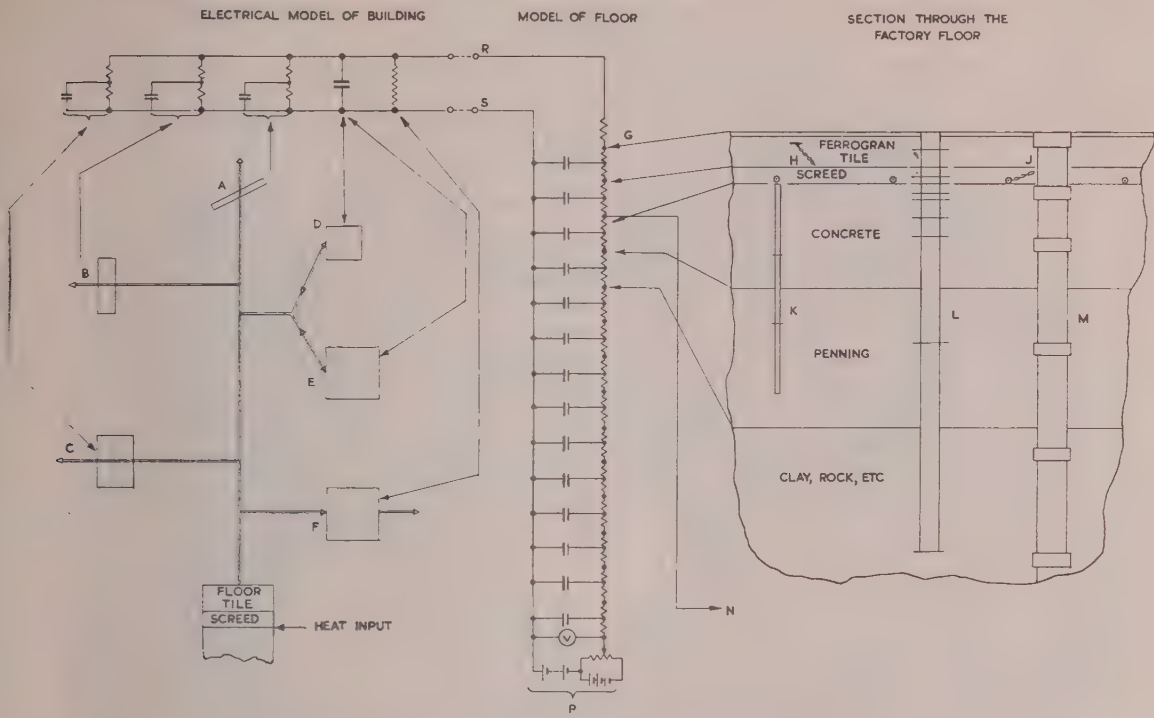


Fig. 2.—Electrical representation of large single-storey factory.

Effect on average square foot of floor of: A. Glass roof.
B. Celotex and aluminium wall.
C. Brickwork.
D. Structural steelwork.
E. Inside air.
F. Loss in ventilation air.
G. Floor-surface heat-transfer coefficient resistor.
H. Heat-flow disc.
J. Thermocouple to heater.
K. 1 ft probe.
L. 2 ft probe.
M. 7 ft probe.
N. Current representing heat input, from constant-current unit.
P. Battery representing difference between surface and deep subsoil temperatures.

consisting of thermocouples inserted into brass discs were used:

(*a*) 7 ft probes with brass discs $\frac{3}{4}$ in thick and 2 in in diameter situated at 7, 6, 5, 4, 3, 2, $1\frac{1}{2}$, 1, $\frac{1}{2}$, $\frac{1}{4}$ ft and at the floor surface. The discs were held in a long Bakelite cylinder filled with a resin mixture to form a solid whole.

(*b*) 2 ft probes with $\frac{1}{16}$ in-thick, 2 in-diameter discs set at 2 ft, 1 ft, 6, 5, 4, $3\frac{1}{2}$, 3, $2\frac{1}{2}$, 1 in and at the surface.

(*c*) 1 ft probes with $\frac{1}{16}$ in-thick, $\frac{1}{2}$ in-diameter discs set at 12, 8, 4 in and at heater level, attached to the heater.

These were set into holes in the ground left vacant during construction and the holes were filled with soil, penning and concrete similar to the main floor area. Probes of type (*a*) were designed for robustness while the others were designed for detailed measurements at around heater level. The thermocouples were led to several 6-point recorders and selected couples were continuously monitored. In addition, full use was made of 'heat-flow discs',[9] all the heat transfer results given here being taken on calibrated discs adhered directly to the floor surface. Also, harnesses of discs were buried just below the surface of the floor and calibrated against surface ones; these form a permanent installation giving a continuous record of the floor heat output, though their interpretation had to be preceded by laboratory experimental work in which discs were inserted in concrete tiles in various ways. Most of the measurements on the factory floor given in the paper were taken in full normal operation but certain of them were purely experimental. Thus the high surface temperatures of Fig. 7 and the heat flows of Figs. 8 and 10 were taken on the middle 4 ft^2 of a heated area covering 600 ft^2 only, this being the only heated area at that time.

(2.2) Existing Factory Conditions

The building when operating has four main sources of heat:

Floor storage heating: 3 MW night and midday; $16\frac{1}{2}$ hours maximum.
Machines: mean full load 0·6 MW continuous; reduced at weekend.
Lighting: mean full load 0·5 MW continuous; reduced at weekend.
Occupation 0·1 MW continuous; reduced at weekend.

To a first approximation the machines and weekend lighting kept the temperature of the shop when occupied to a base value of about 11°–14° F above the ambient temperature. When the shop is unoccupied the floor heating keeps it at about 20° F above the ambient temperature. It follows that the two together can keep the factory at about 32° F above the outside ambient temperature, i.e. well within the accepted recommendation for such a building.*

Temperature variations are small, and the maximum variation of the mean temperature over a period of three weeks of rapidly varying temperature during the coldest period of the year is

* Recommended temperature (I.H.V.E. guide): sedentary work, 65° F; light work, 60° F; heavy work, 55° F.

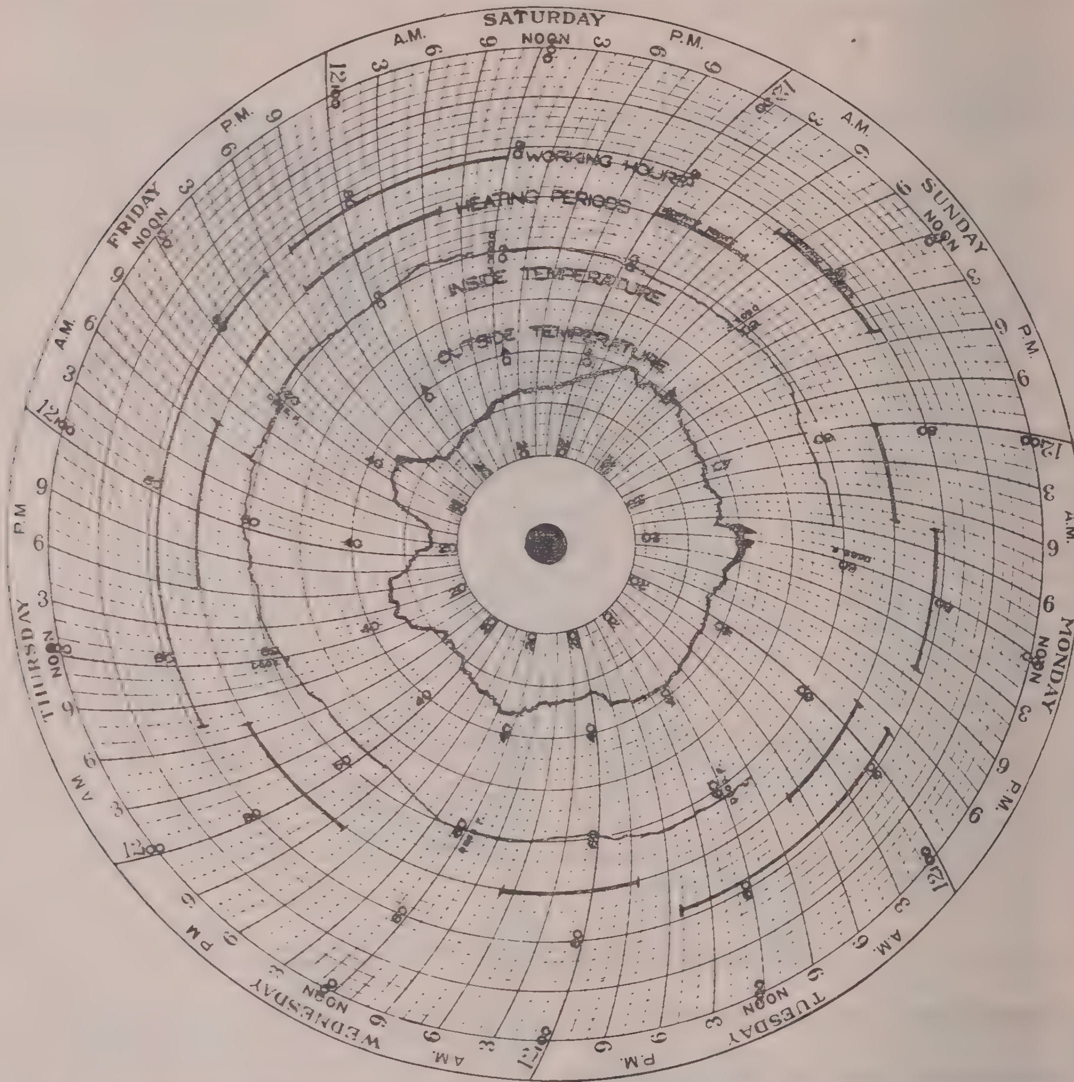


Fig. 3.—Measured winter temperatures inside and outside single-storey factory with heating periods and occupation.

$\pm 3°$F. A more usual value is $\pm 2°$F, which can be maintained either by thermostat or, once the building characteristics are assessed, by simple hand switching at times determined by the late evening temperature. An actual weekly record of the behaviour of the building (controlled by manual switching) is shown in Fig. 3. This particular record was selected as illustrating the effect of normal occupation and severe variations of outside temperature. The air changes must be controlled, which, in practice, means the door in such a building must be kept closed, except when immediately in use, and in the neighbourhood of the door there are variations of temperature. Otherwise the factory retains a reasonably even temperature; 4°F is usually the maximum variation between widely separated parts of the building. Variation with height is very small; there is a steep drop in temperature from the floor within the first 2in and thereafter up to crane height the temperature varies no more than 3°F. This is in agreement with the results of other workers.

It has been possible to meter accurately all the separate inputs to the building—machines, lighting, floor heating and occupation. The air changes have not been known with any precision so that an important piece of data is missing. However, the design figures of the building, both in temperature and energy consumption, are reasonably closely borne out in operation; the efficiency turns out to be a little lower than was anticipated, while the air changes are probably rather fewer. Despite careful metering it seems unjustifiable to suggest any modification to published values of U-factors as applied to large installations.

(3) THERMAL BEHAVIOUR OF OPEN, COVERED AND HEATED GROUND

(3.1) Calculated, Measured and Simulated Temperature Variations

It is possible to calculate on transmission-line principles the temperature distribution with time and depth in open ground due to the propagation of ground surface-temperature variation.

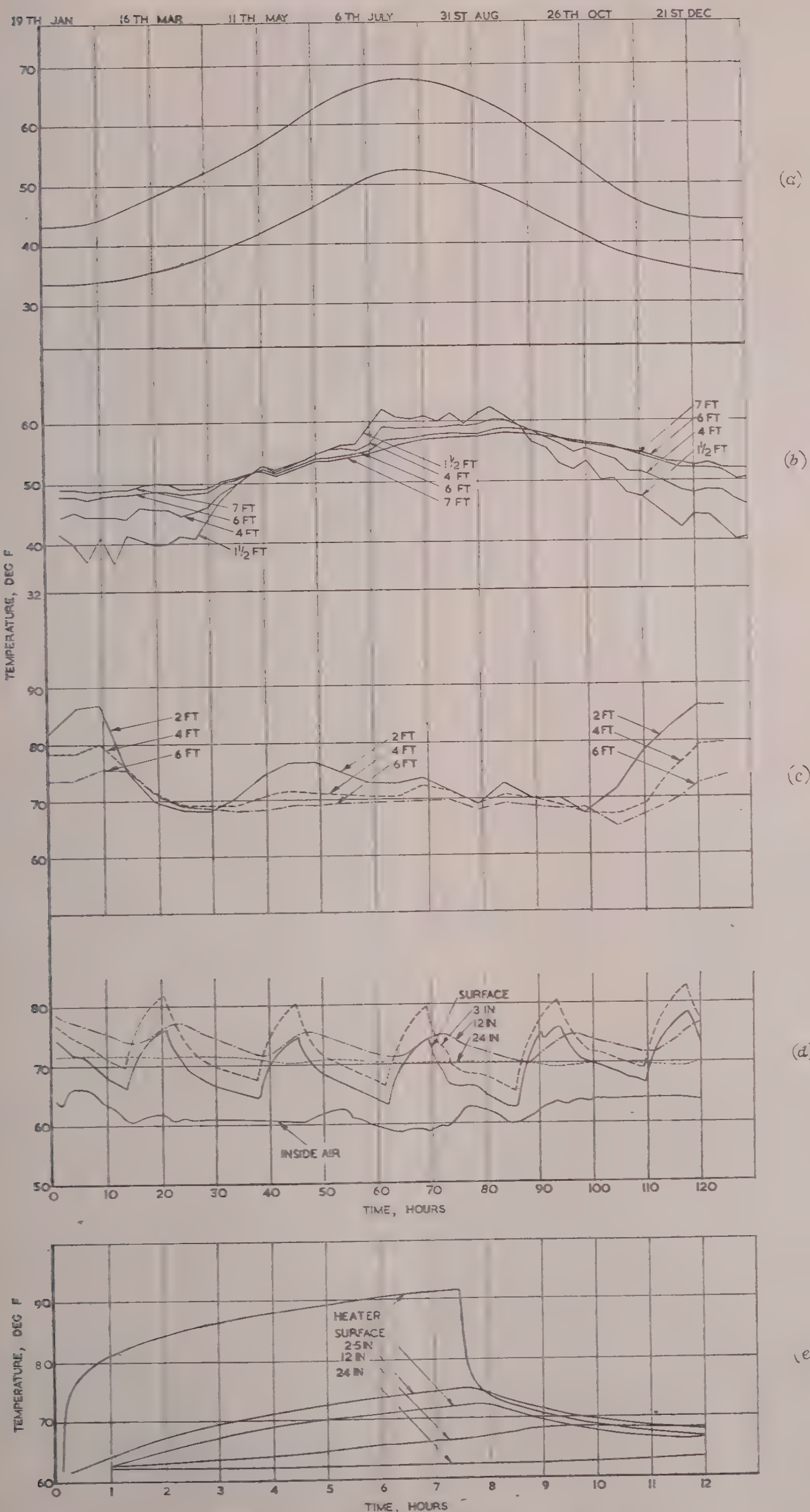


Fig. 4.—Temperatures of single-storey factory.

(*a*) Mean monthly maximum and minimum air temperatures computed for the factory location.
(*b*) Temperatures of uncovered, unheated ground close to factory.
(*c*) Temperatures of covered heated ground.
(*d*) Effect of cyclic heating on ground temperatures 31st Jan.–5th Feb.
(*e*) Ground-temperature variations resulting from single heating cycle.

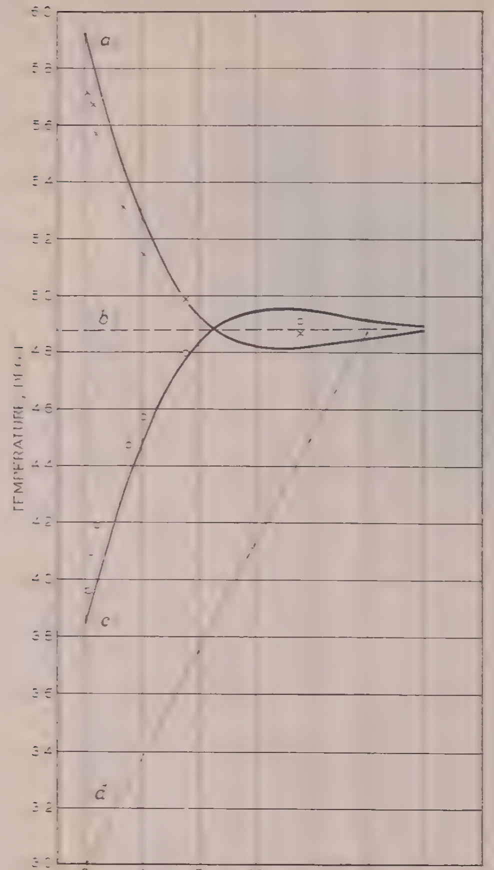


Fig. 5.—Ground temperatures when that of outside air is a maximum or minimum.

—— Calculated from yearly cycle of air temperatures.
× ○ Measured on long-term simulation.
(*a*) Midsummer.
(*b*) Yearly mean air temperature.
(*c*) Midwinter.
(*d*) Linear temperature distribution assumed for simple model.

The relation between open ground surface and air temperature depends on the season. Quoting from Geiger:[10] 'In summer radiation and the earth temperature governed by it determine the air temperature near the ground. In winter, however, when radiation is weak (particularly in the cloudy maritime climate of England) the influence of the ground diminishes. The air temperature is governed by the change of air masses and consequently shows a relatively large monthly range'.

Thus, in the following simple analysis, the ground surface temperature is approximated by mean of maximum and minimum air temperatures. The seasonal variation of air temperature computed for the location of the factory is shown in Fig. 4(*a*), and if this is approximated by a sine function the temperature variation θ in degrees Fahrenheit at a depth d feet below the surface of the ground becomes

$$\theta = 48{\cdot}8 + 10{\cdot}48\varepsilon^{-0{\cdot}173d}\cos\left(0{\cdot}00172t - 0{\cdot}173d\right)$$

where t is the time in days.

This function has a negligibly small amplitude at a depth of approximately 20ft below the surface, and the temperature there is the yearly mean (see Fig. 5).

Measured ground temperatures outside the factory, shown in Fig. 4(*b*), have the form predicted by the above equation, although the variation in mean value with depth suggests thermal leakage from the adjacent building. The ratio of the amplitudes of the measured ground temperature variations lies within 10% of the calculated values. Clearly, the erection of a building will reduce the amplitude of the temperature variation in the ground below it because of the additional resistance between the ground and the heat source. If this occurs when the heat content of the ground is above its mean value, some rise in the yearly mean temperature of the ground will result. The usefulness of the additional heat so stored, during any subsequent heating of the building, is that there will be a slightly more rapid rise in the internal temperature of the building, since the ground absorbs less heat during the transient condition, but there is no increase in the final steady temperature. Calculation shows that the date at which maximum heat is stored in the ground is approximately 7th September, and this should therefore be the best time at which to complete the shell of a single-storey building. Once the cyclic heating is on, there is a long-term rise in the measured ground temperature beneath the building [Fig. 4(*c*)] which swamps the normal winter decrease. The short-term effect of blocks of energy on the daily temperatures is shown in Fig. 4(*d*). This diagram shows the drop in temperature between heater level and surface, the progressively smaller oscillations in temperature as the depth increases and the time differences between peak temperatures at different depths. A more detailed picture of one cycle only, which includes the heater sheath temperature, is shown in Fig. 4(*e*); this again illustrates the phase lag with depth.

The analogue can be made to demonstrate all these points: in particular, the measured form of Fig. 4(*d*) is reproduced in Fig. 6 for the steady cyclic state, i.e. without the effects of long-term temperature changes in the ground or variations in the outside air temperature. As one descends from the heater the cyclic temperature changes are attenuated, phase shifted and changed in shape in a manner analogous to the propagation of a wave along a transmission line, until the daily variation becomes negligible at about 3½ft below the surface of the floor.

A deep-ground yearly mean temperature has to be assumed in the simulation. A value of 45°F given by Billington[11] was first used but was replaced by 48·8°F derived from the local yearly temperature variation of Fig. 4(*a*). The initial assumed temperature distribution was one that results from a uniform heat flow between the deep-ground temperature at 20ft and an outside air temperature of 30°F, giving an approximately linear temperature distribution between these points. That this is pessimistic is shown by the calculated temperature distribution when the mean monthly temperature is at its minimum (Fig. 5).

In order that the magnitude of the time-constants involved may be appreciated, some purely experimental records with continuous floor heating, as distinct from cyclic heating, are shown in Fig. 7. These show ground temperatures in an alternative form, which illustrates how the heat lost by the heater to the ground is slowly filling up a large heat sink and gives some conception of how long this takes. An advantage of this presentation is that it may be used to give reasonable estimates of both heat conduction and the heat stored at any level which the measurements encompass. The unusual initial ground-temperature distribution is the result of the propagation of previous experimental heat pulses.

(3.2) Measured and Simulated Heat Flows in the Factory

The simplest relevant system of heat transfer is one of constant power to a thermal store and of constant ambient temperature. At the first instant all the energy is stored and none passes the

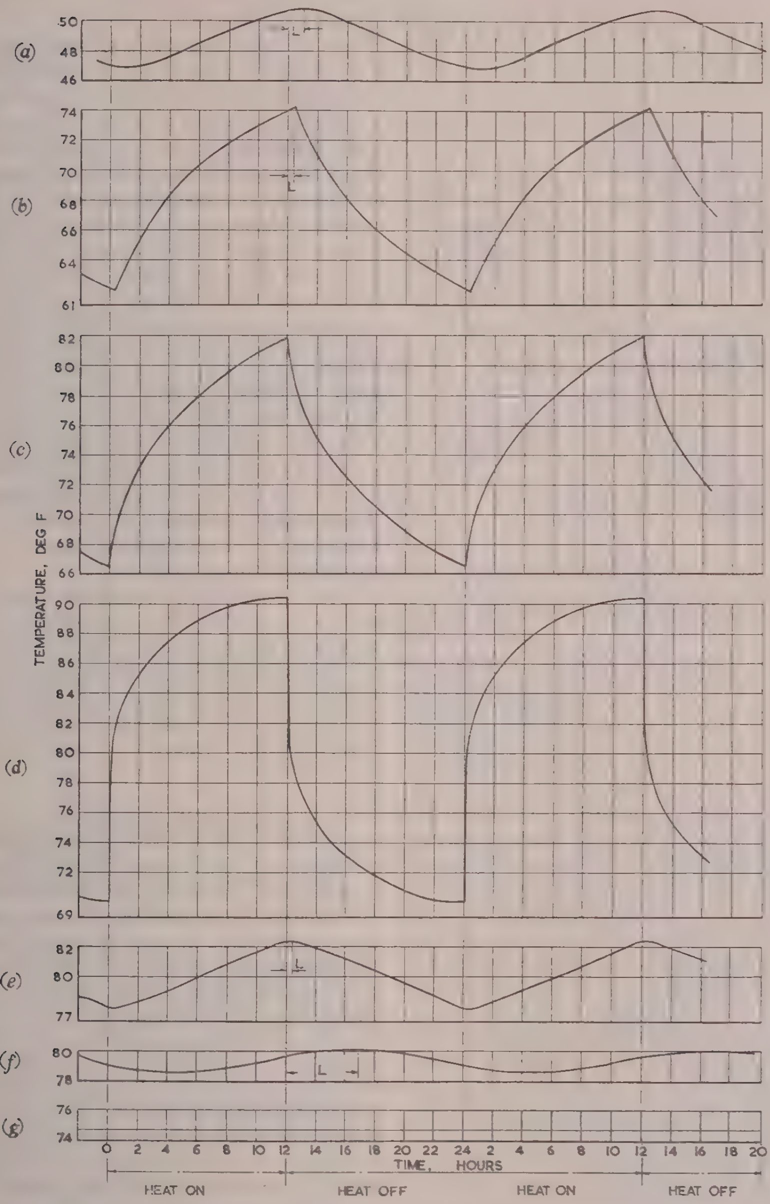


Fig. 6.—Simulated daily temperature variations at various ground levels.

15 W/ft² of heated floor, 12 hours on and 12 hours off. Deep-ground temperature, 45° F. Outside air temperature, 30° F.

(*a*) Inside air.
(*b*) Floor surface.
(*c*) Bottom of floor tile.
(*d*) Heater.
(*e*) Bottom of concrete.
(*f*) Bottom of penning.
(*g*) 2 ft of clay.
L. Lag of peak temperature behind heater.

oundary of the store; after an infinite time all the energy passes hrough the store, which is in equilibrium with the atmosphere. There is clearly a time-constant involved depending upon the pecific heat, conductivity and heat-transfer conditions at the urface. It was possible to obtain an experimental record of his transient for a section of the factory floor. Before the eating was switched on, the ground gradients from heater level o 6 ft deep were within 6° F, while the mean ambient temperature n the building was close to the mean ground temperature. It aried by only ±4° F during the 96 hours that readings were aken, since the heated area was 600 ft² and outside air-temperature changes were small. The uniform initial ground-temperature distribution was again the result of its previous 'history'. Measurements of the power input to the floor, the heat-flow discs giving the heat output of the ground and the below-heater temperatures permitted a thermal balance to be drawn up, shown in Fig. 8(*a*)–(*e*), using published values of ground properties. It should be noted that at the end of this period some 82% of the heat input was going into the building.

These measurements from the factory floor can now be compared with the results of the simulation, also given in Fig. 8; curves (*b*) and (*g*) are directly comparable and are very similar.

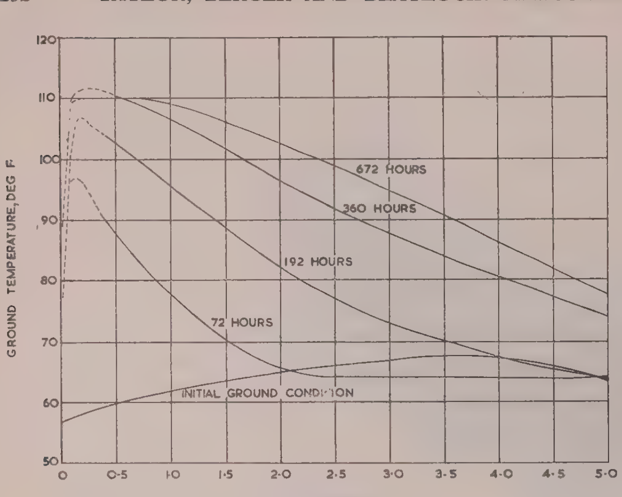


Fig. 7.—Ground temperatures during heating.

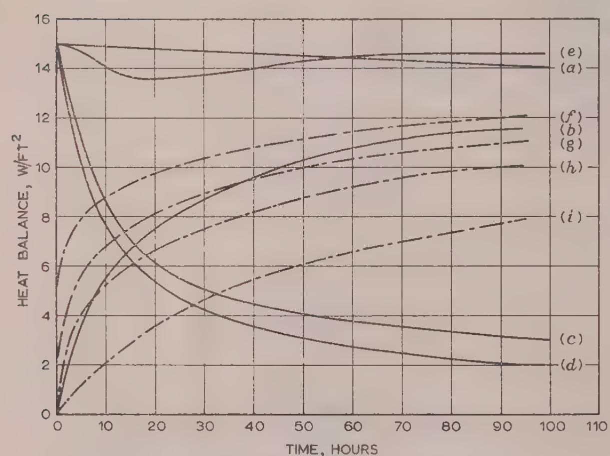


Fig. 8.—Heat balance during continuous heating 12 noon 4th July–9 a.m. 8th July, 1957.

——— Measured results.
(a) Electrical input.
(b) Heat loss from floor surface.
(c)*Heat retained in ground or lost outside heated area.
(d)*Heat retained between surface and 2 ft.
(e) = (b) + (c).
* From ground records and assumed properties.
---- Simulator results.
(At constant input of 14·5 W/ft²).
(f) Heating at surface.
(g) Heater at 2 in.
(h) Heater at 3 in.
(i) Heater at 12 in.

The analogue curves (*f*)–(*i*) also illustrate the effect of continuous heating by heaters at different depths, a parameter that can be easily modified on the analogue but not in practice. In the simulation the constant inside air temperature was set by a constant-voltage unit connected between R and S of Fig. 2.

Returning now to the condition of daily cyclic heating, the analogue results given in Fig. 9 show the principles of floor thermal storage most clearly. The lower diagram shows that in the steady cyclic state the floor output at any time depends remarkably little on whether the heating elements are on or off. It is this feature which is responsible for the small variation in inside air temperature, shown in Fig. 9(*a*). If the same heat cycle were supplied directly to the air only, the mean temperature would be the same but the daily fluctuations would be too great for comfort [Fig. 9(*b*)].

The measurements from heat-flow discs are shown in Fig. 10(*a*) and are very different from those of the analogue. The day-time values fall so low as to suggest poor thermal storage. The heat transfer coefficients, calculated from the measured heat flows [Fig. 10(*a*)] and surface/air gradients [Fig. 10(*b*)], reached their full value only during the hours of complete darkness [Fig. 10(*c*)].

The wide and irregular variation of the day-time heat transfer coefficients, measured in the above way, was due to solar radiation. The heat-flow discs clearly did not measure the total heat flow from the floor surface but only the contribution to it from the thermal store (Fig. 1). Fig. 10(*d*) shows qualitatively, other things being equal, that sunshine reduces or even reverses the heat flow from the thermal store. Quantitatively, by setting up the heat balance so that the sum of the heat-flow-disc reading and solar radiation was equal to the convection and radiation losses from the floor, it was possible to compute the amount of solar radiation reaching the floor and show that over a day it varied as the total bright sunshine. The correlation was based on a daily total since the sunshine recorder was situated about 2 miles from the factory. In making these calculations it was found that the convection constant in the convection loss formula[12, 13] was 0·2.

The effect of solar radiation is that the heat stored in the ground is not going into the factory but remains during the day as stored energy to assist the following night. This can also be shown to be true on a smaller scale for fluorescent lighting over a large area. In the sense that radiant energy to the floor or increased air temperatures above the floor cut down the heat flow from the floor, the power delivered from the storage is self-compensating.

(4) THERMAL PARAMETERS DEDUCED FROM THE ANALOGUE

(4.1) Efficiency of Factory Floor-Heating Installation

From the measured factory heat-flow records, it is possible to set up a heat balance for cyclic heating similar to Fig. 8. Efficiencies of the order of 85% are generally found, but it is not possible to be precise because of variations in ambient temperature and solar radiation. It is indeed this value that can be measured with precision only on a thermal simulator where constant conditions may be maintained.

Results from the simulator are presented in Fig. 11 as traces of the daily mean efficiency variation with time (at a constant outside air temperature). The efficiency of the floor heating is defined as the ratio of the difference between the actual heat flow and the natural heat flow from floor to building to the heat put into floor by the heater; here the natural heat flow is that which would have occurred for the same outside air temperature without the floor-heating installation; in the measured conditions this was negligible. The efficiency in curve (*a*) is shown for the standard 'worst' condition with linear temperature distribution between the outside air at 30°F and a temperature of 45°F at 20 ft, giving 75% of the steady input in approximately 19 days. As previously explained, this is a pessimistic initial ground-temperature distribution, and a second curve shows the transient efficiency for the same outside air temperature but an initial ground-temperature distribution which is uniform at 45°F. This reached 75% efficiency in approximately 7 days. The addition of a building shell to the heated floor structure must clearly lengthen the time-constant of the efficiency transient. The results given in Fig. 8 and 11 [curves (*h*) and (*b*), respectively] show this transient when the inside and outside air temperatures are maintained constant but the change is too small to be

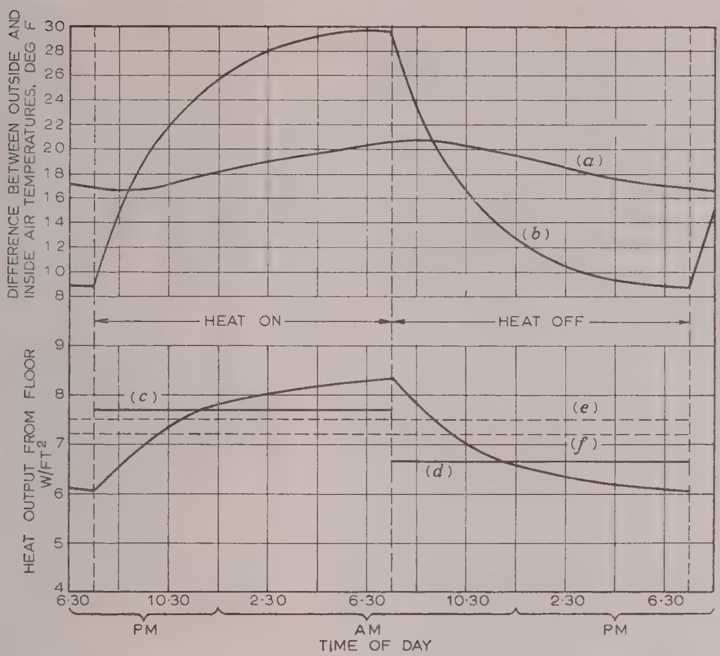


Fig. 9.—Thermal-storage floor heating compared with cyclic air heating.

Temperatures at same heat input: (*a*) With floor heating.
(*b*) With air heating.
Heat flows: (*c*) 'Heat-on' mean level.
(*d*) 'Heat-off' mean level.
(*e*) Mean input to floor.
(*f*) Mean output from floor.

etected. This confirms a direct analogue measurement on the uilding shell without the floor, which shows that, following an brupt change of outside air temperature, 90% of the change in side air temperature is completed in 14 hours. This demonrates that the building shell contributes little to the thermal orage.

Neither of the transient efficiencies of Fig. 11 approximates a steady value within a length of time equivalent to the ormal winter heating period, showing that this period is sufficient for steady conditions to be attained in the subsoil. st as the duration of the winter is insufficient to heat the bsoil, the duration of the summer is insufficient for it to cool its temperature at the end of the summer previous to the st heating cycle. Thus successive heating periods start with a onus from previous periods. Beyond the third year there is measurable change, within the limits of accuracy of the mulation.

(4.2) Thermal Time-Constants

The study of temperature transients for a wide variety of at-input and temperature functions is quickly and elegantly rformed on the model. Any difficulties in reasoning on a ermal level' are reduced since the analogous electrical circuit related to the theory and experience built up in the manipulaon of electrical networks.

The temperature transients predicted for the factory explain e small variation in the measured inside air temperature when e outside air temperature fluctuates in a daily cycle; only a cle of some few days extent could produce an appreciable ange and this can be countered by the thermostatic control ring heating.

It is often argued that the heat stored in the ground beneath single-storey floor-heated building will make the response of the inside air temperature to a sudden fall in outside air temperature slower than that of the same building and floor without floor heating. But the application of the theorem of superposition to the electrical analogue shows that the additional heat released from the floor and structure by the abrupt change in outside air temperature is the same for both cases. The additional heat stored in the floor-heated building is an essential part of the potential distribution which maintains the normal heating output from the floor. The portion of the heat input which flows to the building depends only on the 'network' and the time function of the input. Superimposed on this is the heat flow due to the voltage step between outside air and deep ground, and this flow does not depend on whether the floor heating is on or not. This point is illustrated by the temperature transients obtained on the analogue for three conditions, without heating, with cyclic floor heating and with cyclic air heating (Fig. 12).

Two useful time-constants are suggested. The first is the time-constant of the floor and building, defined as the time for the air temperature inside the building to complete 63% of the change produced in it by an abrupt change of outside air temperature without change of heat input to heaters, the heat flows initially being in equilibrium; this is a property of the building and independent of the method by which it is heated. The second is the time-constant of the heating system, defined as the time in which the heat flow into the air inside a building will complete 63% of the change produced in it by an abrupt change of heat input to the heater without change in outside air temperature; this, the 'smoothing' property of the heat input system, makes the floor heater an effective thermal storage heater.

The analogue measurements give the time-constant of the factory building as 7 days and the time-constant of the heating system as 12 days.

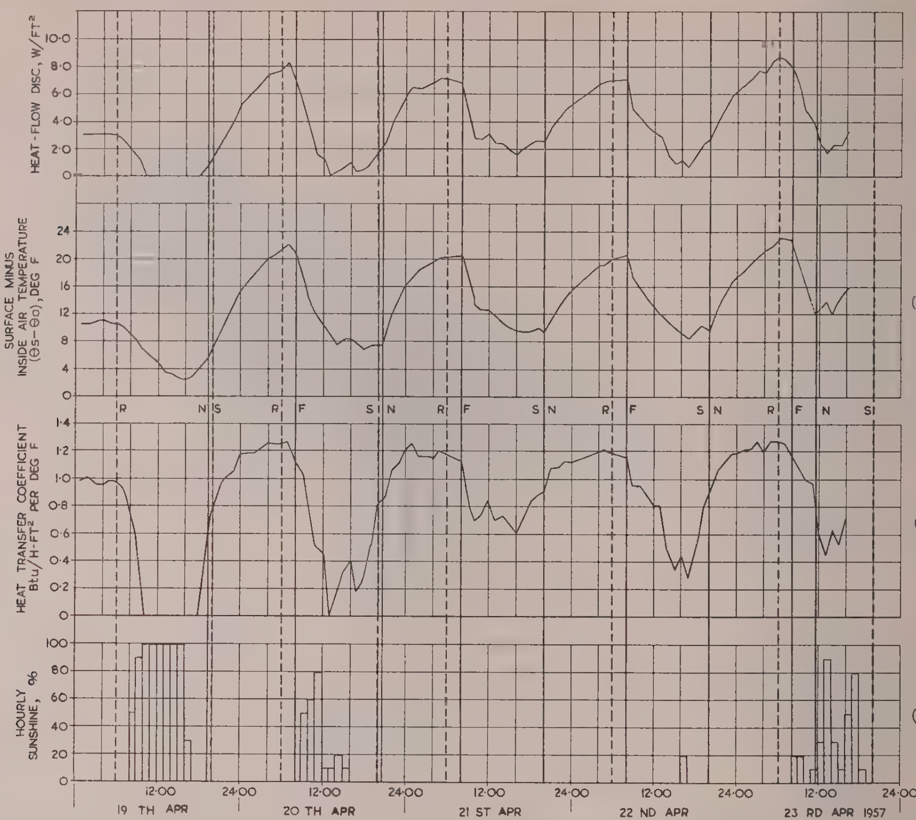


Fig. 10.—Correlation between floor heat flow and solar radiation.

R. Sunrise.
S. Sunset.
N. Heat on.
F. Heat off.

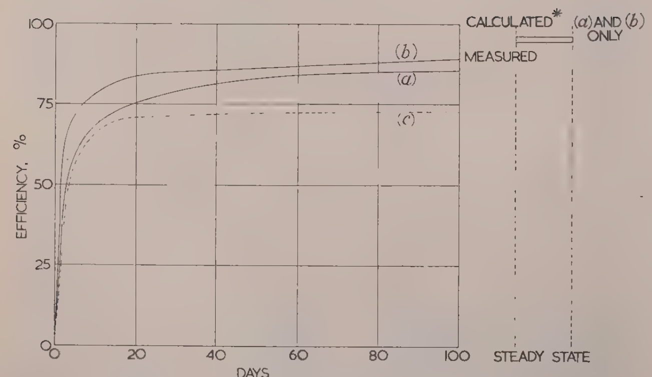


Fig. 11.—Simulated efficiency/time relationships (cyclic heating).

Single storey factory.
(a) Initial ground temperature linear between 30° F at surface and 45° F at 20 ft.
(b) Initial ground temperature uniform at 45° F.
Narrow single-storey building.
(c) Typical yearly heating cycle.

* Steady state calculated from known input and thermal resistance of model.

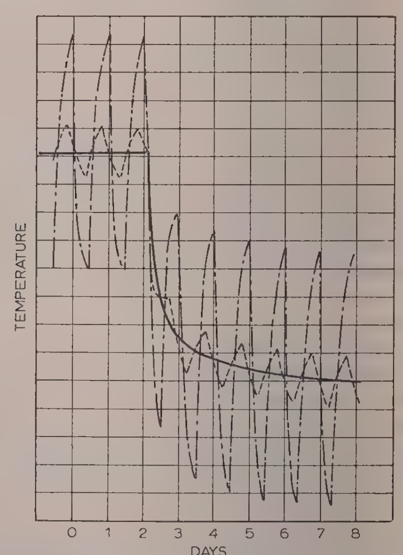


Fig. 12.—Single-storey factory: comparison of transient changes in inside air temperature produced by the same sudden change in outside air temperature for various forms of heating.

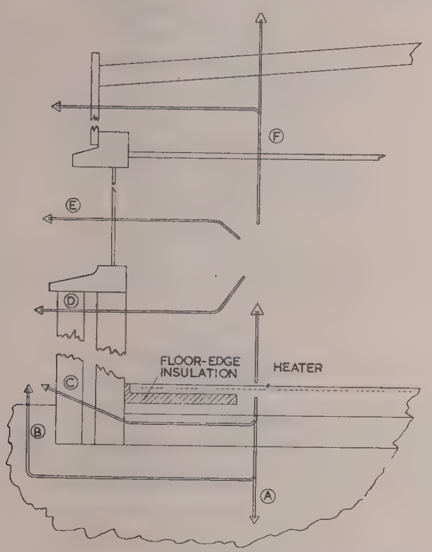
——— No heating.
– – – – Cyclic floor heating.
—·—· Cyclic air heating.

Traces are given to the same arbitrary temperature scale set to the same initial mean level.

(5) SIMULATION OF A NARROW SINGLE-STOREY STRUCTURE

(5.1) Thermal Characteristics of the Building

A smaller building provided a different type of problem. ne building is a single-storey 11 in-cavity-brick-wall structure, 0ft long and 30ft wide, with a very large window area and a eel supported roof composed of thermal insulation, roofing felt d asphalt. Because of the narrowness of the building, heat sses through the ground at the edge of the floor in the direction the shorter dimension of the building were important, and



. 13.—Heat flows in single-storey building: simplified representation of steady state.

	Loss from building kW	%	% of total area
A. Loss into ground	1·19	2·86	33·5
B. From soil outside building to air	6·46	15·51	
C. From wall below floor level to air ..	0·4	0·96	
D. From wall between floor and windows to air	3·63	8·72	12·0
E. Through windows to air	18·19	43·68	19·6
F. Through ceiling to air	11·8	28·32	33·5

e electrical model therefore represented a 2-dimensional cross-tion of the floor, though the shell of the building was repre-ated by the same form of branching unidimensional network as s used for the factory. This also permitted the testing of the ectiveness of floor-edge insulation on the model (Fig. 13). ce the simulation of the factory showed that only the surface of ground formed the heat reservoir involved in daily heat-w variations, the thermal capacity of the floor was represented ly down to 4ft below the surface for initial measurements of ady daily heating and cooling conditions. Below this depth ly the resistance of the ground was represented.

After these measurements the model was considerably elabo-ed to provide more information on the importance of the ound beneath the building, and its actual transient per-mance. Deep-ground capacitors were included, and to nulate actual operating conditions two further refinements re added: variation of the outside air temperature following mean monthly air-temperature contour of Fig. 4(*a*), and nple on/off thermostatic control of the floor heating by inside temperature. To test the thermal behaviour of the structure, ovision was also made for a short cold period in midwinter. e complete model is shown in Fig. 14 and is described in the xt Section.

The measurements in the steady condition with 30° F constant outside air temperature are shown in Fig. 13. Simple changes in the model demonstrated that the proposed floor-edge insulation produced the same rise in inside air temperature as a 5% increase in the heat input. On the other hand, with edge insulation, the floor temperatures at the edge oscillated with greater amplitude than the central floor temperatures, as a consequence of the reduction in the depth of the effective heat-storage reservoir, the insulation effectively restricting its lower boundary. The floor-edge heat losses on the model showed good agreement with those given by Billington,[11] and the reduction in loss produced by the insulation is close to his value for an equal width of vertical insulation. Also, in this particular building, the roof losses were high, and experiments showed that a 4in layer of glass wool on top of the plaster ceiling would save 30% in power during severe winter weather.

The floor-heating efficiency was measured both for the steady-state condition and for the more complete representation. Steady-state measurements gave an efficiency of 75% with no other source of heat than the floor. The more realistic measurements were made for the same condition, by allowing two 'years' to elapse without heating and then switching the floor heating on in November and off in May for several years. The efficiency reached in successive yearly cycles, when these were steady, was $73 \pm 3\%$, which was the order of accuracy of the experiment. A curve of transient efficiency for a typical yearly cycle is shown in Fig. 11. The final efficiency is lower than that of the factory floor and the transient is more rapid because the edge losses are greater and the heat flow into the ground is less important. Other measurements on the complete model showed the form of temperature variation at various depths predicted by calculation (Fig. 5) and compared the variation of inside air temperature produced by the cold period with and without floor heating. These latter results confirmed the theoretical conclusion of Section 4.2.

The layout of the model was controlled by three assumptions:

(*a*) The inside air is at a uniform temperature.
(*b*) The heat loss through the edge of the floor is important.
(*c*) The heat flow in the ground along the axis of the building is negligible over most of its length.

The first assumption implies perfect mixing of the air, equivalent to infinite conductivity, and leads to the representation of the inside air as a point and the shell of the building as a number of unidimensional heat-flow paths in parallel leading from it to the outside air. The two other assumptions enable a 2-dimensional model of a cross-section of the ground and floor structure to be constructed. The heat flows under the ends of the building cannot be represented in this, since the typical section is unaffected by the heat flows at the ends. It follows that the losses from the model cannot be directly rescaled to give the losses from the 'true' building but only from one in which the ends of the floor are perfectly insulated. This could be avoided only by making a 3-dimensional model of impractical complexity. A closer approximation to the real building can be made by regarding the floor-edge losses as losses per unit perimeter and rescaling to give the correct total loss.

(5.2) Some Special Simulation Details

The complete resistance-capacitance model of the single-storey building is shown in Fig. 14. The capacitances required for the deep ground were in the range 200–300μF and as capacitors of this order are usually leaky, a feedback-amplifier analogue was chosen. This consisted of a Miller integrator provided with compensation both for the input resistance of the amplifier and the leakage of the feedback capacitor. To minimize the ampli-

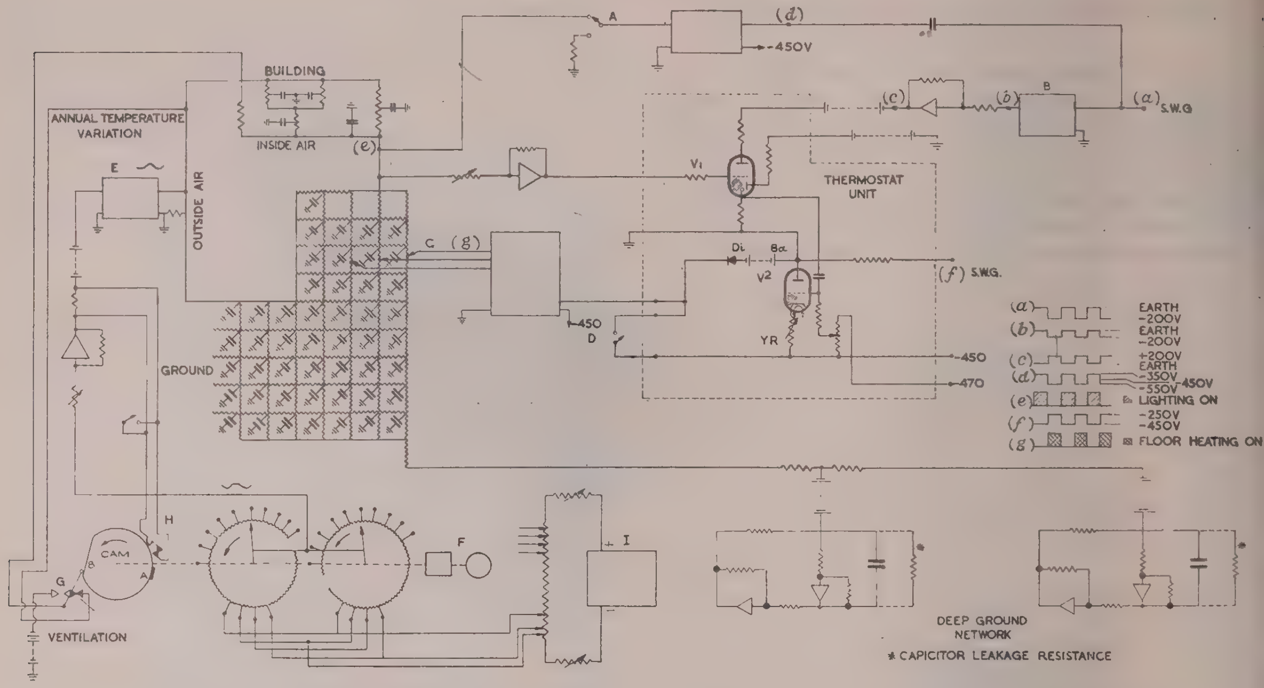


Fig. 14.—Complete analogue of single-storey building and surroundings.

A. Constant-current unit representing lighting, with on/off switch.
B. Constant-voltage unit.
C. Constant-current units representing floor heating.
D. Floor-heating on/off switch.
E. Constant-voltage unit representing atmospheric heat inputs.
F. Synchronous motor and gearbox driving cam and potentiometer. 1 revolution = 1 year.
G. Switch giving ventilation at outside air temperature in summer.
H. Switch giving midwinter cold spell.
I. 200 mA power unit.
S.W.G. Square-wave generator.

fication necessary it was expedient to bring the steady input voltage near to earth potential, using a suitable h.t. battery.

To understand the input circuits it is necessary to know that:

(*a*) Temperatures were represented as negative voltages with respect to earth in the network.

(*b*) The control square-wave generator gave two synchronized outputs, one a negative square pulse with respect to earth and the other a positive square pulse with respect to −450 V; both were of 1 : 1 mark/space ratio.

The varying outside air temperature was derived from a 'round' potentiometer driven through a gearbox by a synchronous motor. A feedback amplifier provided phase inversion at near unity gain and acted as a buffer between the function generator and the constant-voltage unit. The latter is necessary to accommodate the varying impedance presented by the network. A cam on the same shaft as the round potentiometer closed a microswitch, short-circuiting a resistor in the output line of the feedback amplifier. The resulting voltage step, arranged to occur in 'January', thus simulated a sudden cold spell. A second microswitch on the cam arranged that the preheating of the ventilation air to a constant temperature of 58° F ceased when the outside air reached that temperature. The floor heating inputs were derived from three constant-current units controlled by an electronic circuit simulating the thermostat. The inside air was taken as the control point, and the negative voltage was inverted in phase and raised to a suitable value to trigger the cold cathode thyratron. V_1 could be triggered only during the heater-on period since its anode was supplied with a square wave in phase with the control wave to the three constant-current units. If during each such 1/100 sec the inside air temperature exceeded the control value, the thyratron was fired and the control wave to the constant-current units was reduced to zero. The battery B_a had a potential V_B such that

$$V_{2\,burning} + i(VR) - V_B = 0$$

The diode D_i prevented conduction during the 'off' cycle. The lighting to the building was simulated by injecting the square current pulses in antiphase to the floor heating, i.e. lighting was 'on' during the day only and floor heating at night only.

(6) SIMULATION OF A MULTI-STOREY BLOCK

The building studied on the analogue has two wings, each of four storeys of steel and concrete construction, and a complete shell of double glazing in stainless steel, lined by thermal insulation in opaque areas. It has, in addition to floor heating, an air-conditioning system which can provide some additional heating or cooling and a separate refrigeration plant supplying chilled water to thermostatically controlled cooler units suitably disposed throughout the building.[14] In some sections of the building there are large heat inputs formed by the heat losses of apparatus.

The main simplification in the model was that the two wings of the building, being separated by a narrow entrance area, were represented as separate buildings. Each storey was represented as a single unit and internal partitions were neglected, except for their thermal capacity. The heat-transfer resistors for the exposed surfaces (outside walls and roof) were based on the heat-transfer coefficients given in the I.H.V.E. Guide. For the internal surfaces, the upward heat-transfer coefficient was corrected for floor area,[12] whilst the coefficient of the internal vertical walls was taken from the I.H.V.E. Guide. The heat-

ransfer coefficient for a horizontal surface facing downwards, n the presence of one facing upwards, was taken as 1·05 Btu/h-ft^2 er deg F, a correction having been applied to the I.H.V.E. Guide figure based on Danter's paper.[15] The ground beneath ach building was represented to a depth of 4 ft only, since this ncluded the whole of the section affected by the daily tempera-ure cycle. Leakage to the outside air through the ground was epresented by an equivalent loss according to the results of Billington.[11] The floor heating was represented by inputs direct o the appropriate positions in the floor structure. Apparatus nd lighting heat inputs were represented by inputs to the appro-riate inside air positions. Since refrigeration cooling was ecessary only when there was some input to the inside air ositions, it was represented simply by a reduction of the input o the inside air. The ventilating air is preheated to 58° F so hat one end of the resistor representing the air changes must be eld at a constant potential. The current flow into this potential ource measures the heat lost owing to the ventilation.

The change in the relative proportions of the model reflects he change in importance of the various parts of the building. Storage in the ground contributes little to the building tempera-ures. The principal subjects of interest were:

(*a*) The effective thermal inertia of the multi-storey structure, which for off-peak eating governs the temperature variation in the building.
(*b*) The necessary distribution of the total heat input to the building amongst the arious floors.
(*c*) The proportions of the heat losses through the various components of the shell f the building.

The model was heated to give 65° F mean air temperature except in the basement, which is only used for storage) by arious combinations of floor heating and heat loss to the air, nd the required input and resulting amplitudes of temperature ariation were measured. Fig. 15 and Table 1 show the sort f results that may be obtained.

A heating installation must clearly be designed on the basis f the 'permanent' heat sources. The floor heating and lighting oads are of such a nature, whereas the industrial load is not. Results of the type shown in Sections 2 and 3 of Table 1 give a ealistic estimate as to whether the floor-heating capacity has been correctly chosen. When it is, the floor heating on its own should be insufficient to maintain 65° F, whilst with the lighting load added it should not be fully extended, and the temperature swings should not exceed ±4° F.

In Sections 4 and 5 of Table 1 estimated industrial loads are included. Utilizing as little heating and as little cooling as

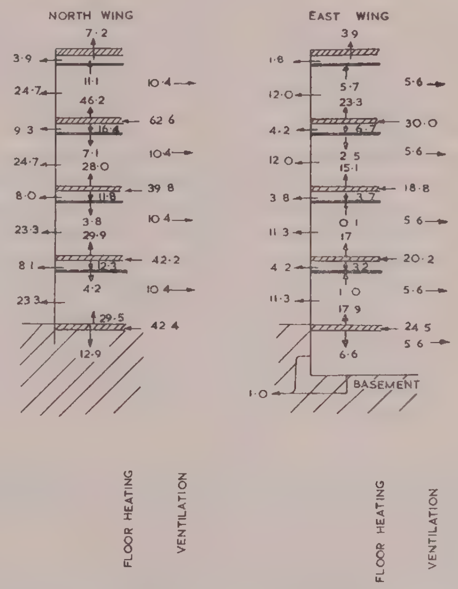


Fig. 15.—Heat flow through a four-storey building.

Results in kilowatt-days per day.
Outside air, 30° F; deep ground, 45° F.
Ventilation represents 2 air changes per hour at 58° F.
Cyclic floor heating inputs adjusted to maintain a mean inside air temperature of 65° F.
No other input.

Table 1

ANALOGUE DESIGN STUDY FOR MULTI-STOREY BLOCK

	Floor	Section 1					Conditions in building when it is heated to 65° F mean temperature by various methods assuming two ventilation air changes per hour at 58 F. Section 2		Section 3		Section 4				Section 5		
		Floor heating installed	Lighting load		Industrial load		Floor heating only		Floor heating and lighting		Floor heating or refrigeration with lighting and industrial load				Floor heating and refrigeration with lighting and industrial load for swings less than ±4° F		
			Day	Night	Day	Night			Floor heating utilization		Floor heating utilization	Refrigeration Day	Refrigeration Night		Floor heating utilization	Refrigeration Day	Refrigeration Night
		kW	kW	kW	kW	kW	kW	deg F swing	%	deg F swing	%	kW	kW	deg F swing	%	kW	kW
North wing	Ground	72	36	0	26·5	10	85	±3	71	±2·5	23	0	0	±4·5	33	8	0
	1st	90	36	0	23·5	13	84	±2	53	±3	0	2	2	±6	16	16	0
	2nd	90	36	0	38	35	80	±2	48	±3	0	15	15	±4	0	38	0
	3rd	99	36	0	50	0	125	±3	83	±3	18	0	0	±8	67	15	0
East wing	Basement	0	0	0	0	0	—	67° F mean ±1·5	—	62° F mean ±1·5	—	—	—	59° F mean ±·5	—	—	—
	Ground	36	24·3	0	75	75	49	±3	62	±3·5	0	66	66	±4·5	0	78	53
	1st	36	24·3	0	79	63	40	±2	47	±3·5	0	65	63	±7	0	85	44
	2nd	36	24·3	0	34	0	38	±2·5	41	±3·5	0	19	0	±7	27	30	0
	3rd	54	24·3	0	30	0	60	±3·5	60	±3·5	0	4	0	±9	49	25	0

possible, the temperature swings are as much as $\pm 8°$F, but to keep the swings to $\pm 4°$F refrigeration and floor heating are both needed.

The actual building has been in operation during a winter, and from temperature records and reports from its occupants its performance has been satisfactory and as planned.

(7) CONCLUSIONS

The temperature records of a single-floor factory show that a large floor-heating installation of 3 MW capacity, together with some industrial gain, has given satisfactory conditions from the point of view of thermal comfort. The records are not regarded as providing information that can be used for commenting on published values of U-factors.

The thermal behaviour of the factory and the floor is corroborated by an electrical analogue, and it follows that this can be used to design storage-heating installations. The paper gives examples of the techniques as applied to a single-storey and a multi-storey building.

Certain factors of storage-heating installation such as efficiency, amplitude of temperature swings, heat flows, transient response, etc., in a complicated structure can be assessed only by an analogue. Parameters of various storage arrangements, say using different materials or with mixed heating systems, are probably more readily assessed by an analogue than in any other way.

In the authors' opinion there should be in existence and available for the use of heating engineers a versatile analogue on which the solution of building storage-heating problems should be encouraged.

(8) ACKNOWLEDGMENTS

The authors acknowledge the assistance of their colleagues especially Mr. P. Lumsden and Mr. E. Clark. The Geography Department of King's College, Newcastle upon Tyne, has been most helpful in providing local meteorological data. The paper is published with the permission of the Directors of C. A. Parsons and Co. Ltd.

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DISCUSSION ON 'AN EXPERIMENTAL STUDY OF SURGES AND OSCILLATIONS IN WINDINGS OF CORE-TYPE TRANSFORMERS'*

Dr. P. A. Abetti (*Massachusetts: communicated*): Section 9 of the paper discusses the spatial distribution of oscillations in transformer windings, and points out that these are not sinusoidal. This fact was established, both theoretically and experimentally, several years ago and has been published in papers[A, B, C] not quoted by the author. For instance, his Fig. 13 is almost identical with Figs. 13 and 15 of Reference A and with Fig. 6 of Reference B. Measured spatial distributions of voltage at the higher harmonics are shown in Figs. 21 and 22 of Reference C. These two diagrams also include spatial distributions computed by means of a linearly graded mutual-inductance function. Agreement between measured and calculated values is satisfactory in all cases.

* WHITE, E. L.: Paper No. 3286 S, October, 1960 (see **107** A, p. 421).

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Mr. E. L. White (*in reply*): The comparison given in the paper between experimental results and calculations of the spatial voltage distribution based on an assumed exponential distribution of mutual inductance is believed to be new. In the experiments to which Dr. Abetti refers, and also in those described in the paper, forced oscillations were produced by injection of a current into a winding at one selected point, hence the resulting distributions of voltage could only approximate to those due to free oscillations.

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June 1961

A METHOD OF MEASURING LOSS DISTRIBUTION IN ELECTRICAL MACHINES

By A. J. GILBERT, B.A., Associate Member.

(*The paper was first received* 8*th August*, 1960, *and in revised form* 16*th January*, 1961.)

SUMMARY

The paper discusses the need for new measurement techniques for the investigation and evaluation of machine behaviour. The disadvantages of former methods of core-loss measurement are dealt with, and an improved thermal method is described, based on the principle that the initial rate of rise of temperature at any point is proportional to the loss generated at that point. It is shown theoretically that, to achieve sufficient accuracy, measurements must be made over a short period of time, and this requirement is met by the use of a chopper-type d.c. amplifier and recorder to measure the e.m.f. generated in thermocouples. The measuring equipment is described and the effects of stray a.c. pick-up are discussed. Examples of applications are given which show that not only can the core-loss distribution be measured but also the effects of changes of geometry or materials can be studied by tests on a single production machine.

(1) NEED FOR NEW MEASUREMENT TECHNIQUES

A new outlook on machine development has emerged in the last ten years. The computer has enabled much more complex calculations to be used in machine design, so that engineers are now seeking more exact methods of calculating machine behaviour. As a result new measurement techniques have had to be developed to supplement the normal factory test methods.

The paper describes one such technique, a thermal method of measuring core loss in machines. It was developed as part of an investigation into open-circuit core losses in salient-pole synchronous machines.

One of the fundamental disadvantages of production tests in the factory is that tests to measure the open-circuit or short-circuit core loss can indicate only the total loss in the machine. The designer is consequently unable to obtain any information about the distribution of loss in various parts of the machine, except by elaborate and dubious experiments on machines in which the geometry can be systematically varied. The method to be described overcomes these disadvantages and enables core-loss-distribution measurements to be made on a single production machine.

(2) THEORY OF THE METHOD

(2.1) Principle of the Method

It is well known that if a unit function of heat generation is applied to a medium under steady-state conditions, the instantaneous initial rate of rise of temperature at any point is directly proportional to the heat generated at the point. Thus if a temperature/time curve is taken at a point during the period immediately before and after applying the unit function, the change in slope at the instant of applying the function will be a measure of the heat generation at the point.

(2.2) Previous Work

This principle has been applied by previous workers, notably Laffoon and Calvert,[1] and Smith,[2] to determine the loss in the cores of synchronous machines by suddenly applying the field and taking temperature/time curves. Their conclusions, however, are open to question (in particular the 4-figure accuracy implied by Laffoon and Calvert) if only because of the method of measurement they employed.

These workers used an ordinary potentiometer and galvanometer, so that their readings could be obtained only at intervals of about 40 sec, a method which leads to insufficient accuracy in the measurement of loss distribution, as will be shown in Section 2.3.

(2.3) Measurement of Temperature Rise in a Field of Non-Uniform Heat Generation

The pattern of loss (and therefore heat) generation will determine the temperature distribution in a thermally conducting medium.

In order to obtain some idea of the effect of loss distribution on temperature, an idealized solution of the heat-flow equation for a thermally insulated slab was considered. In Section 10.1, a solution relating time, temperature and thermal conductivity is derived for the points of highest and lowest loss in a slab thermally insulated and of finite thickness in the x-direction and infinite in the y- and z-directions. A linear distribution is assumed over the thickness of the slab.

From these equations is derived the error between the true loss and that obtained by taking the average slope of the time/temperature curve over different periods of time measured from the instant of applying the unit heat-generation function.

Numerical results were evaluated for different ratios of highest to lowest loss for slabs of different thicknesses and thermal conductivities. The values of thermal conductivity used were those applicable to a stator core along and across the varnished laminations.

Fig. 1 shows a curve of calculated percentage error plotted against time over which the slope is measured for a packet of laminations 1 in thick with a ratio of highest to lowest loss of

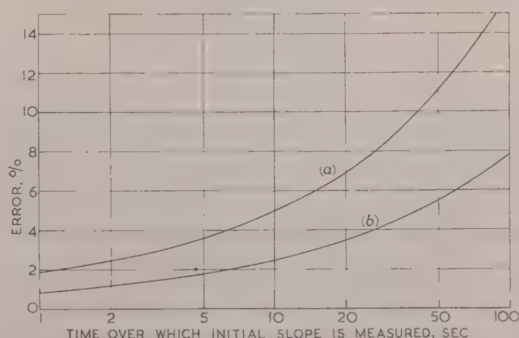

Fig. 1.—Variation of the error between actual and measured rate of rise of temperature with time over which average slope of temperature/time curve is measured.

(*a*) Positive error in region of lowest loss.
(*b*) Negative error in region of highest loss.
2:1 loss distribution over distance of 1 in at right angles to plane of laminations.

*

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
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2 : 1 along an axis perpendicular to the plane of the laminations. Curve (*a*) represents the positive error in the region of lowest loss, and curve (*b*) the negative error in the region of highest loss. These curves are plotted for typical values of axial loss distribution in a stator core in the vicinity of a radial cooling duct or the end of the machine.

Similar curves can be plotted for the loss distribution in various parts of the stator of an a.c. machine, for the thermal conductivities of the solid core in the end structure and of the laminations in the axial and radial directions. Only by measuring the infinitely small change of temperature during an infinitely short time can the true loss at a point be obtained, but these curves show that, provided that measurements are taken over a period of not more than 30 sec after applying the unit function of heat generation, satisfactory results can be obtained from thermocouples placed approximately $\frac{1}{4}$ in apart in the axial direction or 1 in apart in the radial direction. In fact, the loss obtained from measurements taken over a finite time represents the mean loss over a diffuse region surrounding the point.

Thus, although the results of Laffoon and Calvert and Smith mentioned earlier probably bear some relation to the average loss over a region, the time interval between their readings was too large to obtain accurate measurements of local loss distribution.

(2.4) Errors due to Loss of Heat to Surroundings

The curves of Fig. 1 were obtained by assuming that the region considered is thermally insulated. In practice, however, there will be a loss of heat which will be proportional to the difference in temperature between the region and its surroundings. The loss of heat due to the mean temperature difference is allowed for by the cooling curve, and the rise of temperature during the test period of 30 sec will be so small that the additional loss to the surroundings can be ignored, particularly at points which are remote from the surface of the material.

It can be shown (Section 10.2) that, if Newton's law of cooling holds, the percentage error due to heat loss can be expressed as

$$\frac{100(\theta_H - \theta_c)}{2(\theta_F - \theta_a)} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where $\theta_H - \theta_c$ is the temperature rise from the start of the cooling curve to the end of the heating curve, and $\theta_F - \theta_a$ is the steady-state temperature difference between the point and the outside air.

Typical values for a machine are $\theta_H - \theta_c = \frac{1}{3}$°C and $\theta_F - \theta_a = 20$°C, giving an error due to heat dissipation of less than 1%.

(3) MEASURING EQUIPMENT

(3.1) Choice of Thermocouples

Experience has shown that copper-constantan thermocouples give the most satisfactory results. Enamel-covered wires, 0·018 in in diameter, in a single glass sheath have sufficiently small thermal capacity and adequate mechanical strength to withstand the pressures in the cores of machines.

In order to obtain a satisfactory thermal response it is essential to solder the junction to the point at which the loss is to be measured. Fig. 2 shows a sectional view of the method of burying thermocouples in a machine core.

The thermocouples are soldered to laminations or to the end structure before the core is assembled, and the adjacent laminations are slit in order to bring the leads out without disturbing the build-up of the core. Although this will disturb the local flux and loss distribution, the effect on the results will be slight since only a very small amount of material is removed, and as discussed in Section 2.3, the loss measured is the mean

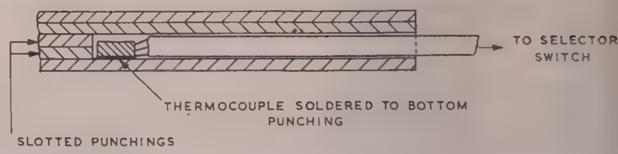

Fig. 2.—Sectional view of a thermocouple buried in a machine core.

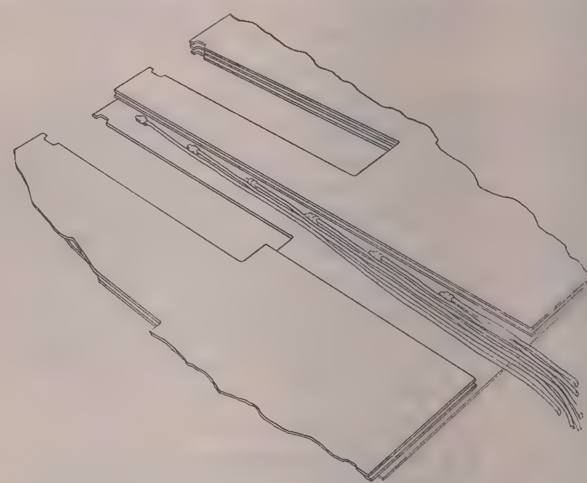

Fig. 3.—Typical arrangement of thermocouples on a stator lamination.

loss over a diffuse region around the point. Fig. 3 shows the arrangement of a group of thermocouples along a tooth and the back part of a segmental lamination for an 80 MW water-wheel alternator in which over 300 thermocouples were placed during core building. When a machine is erected for test the thermocouples are connected by larger-section extension leads to all-copper selector switches fitted with draughtproof covers.

(3.2) Cold-Junction Thermocouple

When using thermocouples it is customary to offset part of the thermo-e.m.f. of the hot junction by a cold junction kept at a known constant temperature which is used as a reference. In this application, as the machine under test warms up, the test thermocouples will reach different mean temperatures and the cold-junction thermo-e.m.f. must be adjusted to keep the readings on a reasonable scale, so that the small temperature rise during the heat-generation period may be accurately observed. It is obviously not practicable or necessary to vary the temperature of the cold junction to suit each thermocouple as it is selected. It is not necessary, except in the case of I^2R-loss measurements, to know the actual temperature, as only the change is required. The junction is therefore immersed in oil in a vacuum flask and connected in series with a stabilized d.c. reference unit, the output of which can be varied to offset the mean thermo-e.m.f. of the selected thermocouple. Fig. 4 shows the circuit diagram of the reference unit.

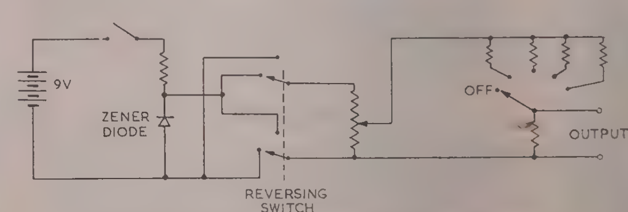

Fig. 4.—Circuit of stabilized d.c. reference unit.

The output from a 9 V grid-bias battery is stabilized against hort-term drift by a Zener diode and a resistor. The voltage across the diode is fed through a reversing switch to a potentio-neter and resistor network giving a voltage output which can be varied from 0 to 60 mV over four ranges. The output is aken across a 10 Ω manganin non-inductively-wound resistor, nanganin being chosen to give negligible thermo-e.m.f. in contact vith copper.

(3.3) Amplifier and Recorder

The need to take time/temperature curves over the first half minute after applying the unit function precludes the use of a conventional potentiometer because of the time taken to obtain a balance. Some form of continuous recording is therefore necessary.

A typical loss generation in a machine when measured with a copper-constantan thermocouple causes a rate of rise of thermo-e.m.f. of 36 μV/min. The device used for recording such low voltages must be driven by a d.c. amplifier having the following characteristics:

Low noise level and negligible drift.

Ability to reject a.c. pick-up.

High input impedance so that its sensitivity will be unaffected by variations in length (and therefore resistance) among the thermocouple leads.

A response adequate to follow the highest initial rates of rise in regions of maximum total loss.

Robustness and ability to withstand mechanical vibrations found in a factory and electrical overloading.

Of the amplifiers at present commercially available a chopper-ype d.c. amplifier is the best compromise.[6,7]

(3.4) Effect of Stray A.C. Pick-Up

The chopper in most commercial amplifiers operates at 50 c/s so that any stray 50 c/s e.m.f.s induced in the thermocouple are iable to appear as a d.c. output. If the machine under test is running (as often happens) at some frequency near 50 c/s the stray e.m.f. will appear as a beat frequency.

The following precautions must therefore be taken:

Inductive loops should be avoided and leads screened where practicable.

Since the thermocouple is soldered to the point of measurement it will be at earth potential and no other earths should be applied to the system.

The resistance between the earthy side of the input to the amplifier should be made as low as possible by connecting the copper lead of the thermocouple to the earthy terminal of the amplifier.

(4) METHOD OF TEST

A time/temperature curve is obtained for each thermocouple over a period of about 30 sec before and 30 sec after the change n loss. The machine under test must be driven by a motor at a speed corresponding to its normal running frequency. For an a.c. generator the cooling curve is obtained with the machine running light without field and the loss is generated by applying he field suddenly.

(5) APPLICATIONS

The method was developed primarily for the investigation of open-circuit core losses in alternators, but it has been used successfully for the measurement of short-circuit core losses in alternators and core losses in transformers and also with limited success for measuring eddy-current losses in the end-windings of alternators.

It could also be used for measuring losses in the rotors of machines, although the practical difficulties here would be considerable, involving the development of a suitable amplifier for mounting on the rotor shaft or slip rings with very low noise levels, or the use of some other device, such as the thermistor, for measuring the temperature.

Not only can the distribution of losses be measured but it follows that the effect of detail changes of structure can be studied on a single machine.

On the waterwheel alternator already mentioned the method was used to investigate the comparative effects of magnetic and non-magnetic duct spacers, magnetic and non-magnetic end-plates and fingers, split and normal stator teeth and stepped and unstepped core ends, under both open- and short-circuit conditions. In addition, dummy strips of copper of the same dimensions as the individual winding strands with thermocouples attached were strapped to the end-windings to determine the approximate loss due to eddy currents on open-circuit.

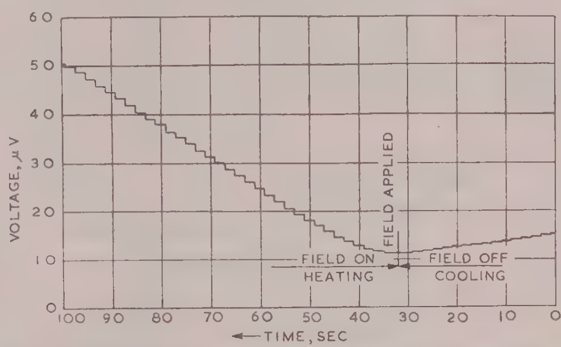

Fig. 5.—Curve of e.m.f. against time obtained from thermocouple buried in the core of a large alternator.

Fig. 5 shows a typical curve measured with a scissor-type recorder for a thermocouple buried in an alternator under open-circuit conditions. Time is measured from right to left. The stepped record is a feature of this particular recorder, which balances itself every 2 sec. The right-hand part of the trace was obtained with the alternator field off and the left-hand part after the field had been applied.

Fig. 6 shows the core-loss measured at points approximately $\frac{3}{8}$ in apart in an axial direction between the stepped end of the core and the first radial duct. Curves are shown for points at different distances from the air-gap. The letters on the curves correspond to the positions of the thermocouples. There is a marked increase in loss at the ends of the packet.

Fig. 7 shows the variation of loss from the gap to the back of the stator core along the radial centre-line of a tooth.

(6) ACCURACY

It will be noticed that all the measured points lie within 8% of a smooth curve. When analysing results it is essential to remember that the loss inferred from the rate of rise is the loss over a diffuse region around the point of attachment of the thermocouple as mentioned above. An estimate of overall accuracy can be obtained by integrating the values of loss measured thermally over the whole machine, and comparing the integrated loss with the total loss measured directly by (for example) input to a driving motor.

For one waterwheel alternator tested and for the 80 MW machine referred to earlier the difference between the losses measured by the two methods was less than 1%. Although the error in the measurement of individual points may be as much as 10%, the overall accuracy is generally better than 5%.

An accuracy of this order may not appear sufficiently good to those accustomed to precise academic standards, but hitherto,

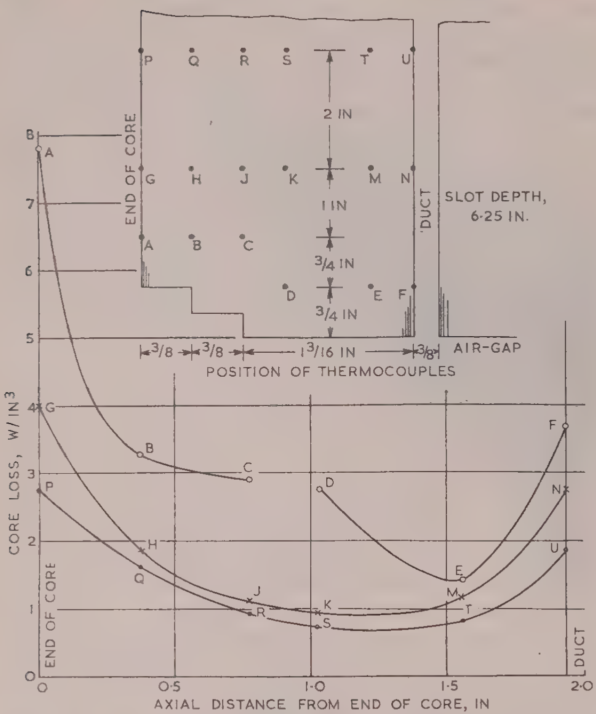

Fig. 6.—Positions of thermocouples and loss distribution in the end-packet of laminations in an 80 MW waterwheel alternator.

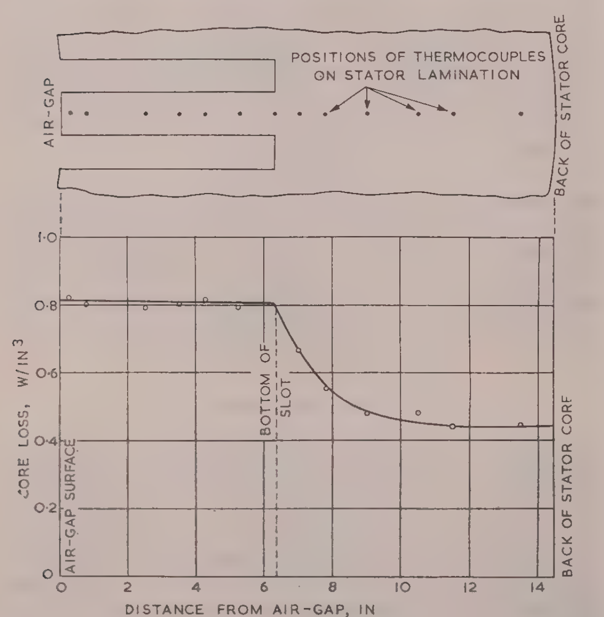

Fig. 7.—Radial-loss distribution in an 80 MW waterwheel-alternator stator lamination.

Core loss measured along radial centre-line of a stator tooth punching placed at middle of 1⅞ in packet.

to the best of the author's belief, this information could not be obtained at all. Consequently, results even to an accuracy of 5% are extremely valuable.

(7) CONCLUSIONS

A method has been described by which the distribution of core loss in a machine can be measured with a satisfactory degree of accuracy, subject to certain limitations, the most important of which are the thermal conductivity and the shape of the loss-distribution curve. The method can be applied only where a rapid transition can be made from the no-loss to the loss condition, and it is particularly suitable for the stators of a.c. generators where the field can be applied suddenly.

Since it is now possible to determine how the loss is distributed in a machine, one of the fundamental limitations on the measurement of open-circuit and short-circuit core loss has been removed.

Hitherto, any attempts to assess the effect on the core loss in a machine of changes of geometry or of materials have involved a long series of expensive and dubious tests on duplicate machines in which these properties were systematically varied. Any change in loss was difficult to assess, as conventional testing methods were not sufficiently accurate. Moreover, duplicates of the largest machines, for which the evaluation and reduction of loss are most important, are seldom tested fully in the factory, so that tests of this nature are out of the question. By using the method described in the paper, however, it is now possible to assess the relative effects of many changes of geometry or materials by tests on a single production machine more quickly, more accurately and more economically than before.

(8) ACKNOWLEDGMENTS

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(10) APPENDICES

(10.1) Temperature Rise with Variable Heat Generation given as f(x)

Consider a slab of material of width d in the x-direction, infinite in the y- and z-directions, and thermally insulated on both sides.

Assume the heat generated per unit volume per second is $Ex + D$ watts per cubic centimetre.

Let θ = Temperature after time t seconds, deg C.

λ = Thermal conductivity, W/cm^2 per deg C/cm.

ρ = Density, g/cm^3.

c = Specific heat, J/g per deg C.

Then diffusivity $h^2 = \lambda/\rho c$ and the heat-conduction equation is

$$\frac{\partial\theta}{\partial t} = h^2\nabla^2\theta + \frac{Ex + D}{\rho c} \quad . \quad . \quad . \quad . \quad (2)$$

For one-dimensional flow,

$$\frac{\partial\theta}{\partial t} = h^2\frac{\partial^2\theta}{\partial x^2} + \frac{Ex + D}{\rho c} \quad . \quad . \quad . \quad . \quad (3)$$

If perfectly insulated boundaries and zero initial temperature everywhere are assumed,

$$\frac{\partial\theta}{\partial x} = 0 \text{ at } x = 0 \text{ and } x = d$$

$$\theta = 0 \text{ when } t = 0 \text{ for all values of } x.$$

Taking Laplace transforms,

$$h^2\frac{\partial^2\theta}{\partial x^2} + \frac{Ex + D}{\rho c} = p\theta \quad . \quad . \quad . \quad . \quad (4)$$

i.e. $$\theta = \frac{Ex + D}{p\rho c} + A\cosh\frac{x\sqrt{p}}{h} + B\sinh\frac{x\sqrt{p}}{h} \quad . \quad (5)$$

Inserting the boundary conditions, it can be shown that

at $x = 0$ $$\theta = \frac{D}{p\rho c} + \frac{Eh}{c\rho p^{3/2}}\,\frac{\cosh\dfrac{d\sqrt{p}}{h} - 1}{\sinh\dfrac{d\sqrt{p}}{h}} \quad . \quad . \quad . \quad (6)$$

at $x = d$ $$\theta = \frac{Ed + D}{p\rho c} - \frac{Eh}{\rho c p^{3/2}}\,\frac{\cosh\dfrac{d\sqrt{p}}{h} - 1}{\sinh\dfrac{d\sqrt{p}}{h}} \quad . \quad . \quad (7)$$

It is now required to find the inverse transform of this expression.

The function

$$\frac{\varepsilon^{pt}\left(\cosh\dfrac{d\sqrt{p}}{h} - 1\right)}{p^{5/2}\sinh\dfrac{d\sqrt{p}}{h}}$$

has double poles at $p = 0$ and simple poles at $d^2p/h^2 = -n^2\pi^2$. By evaluating residues at $p = 0$ and $p = -n^2\pi^2h^2/d^2$,

at $x = 0$ $$\theta = \frac{1}{\rho c}\left(D + \frac{Ed}{2}\right)t - \frac{Ed^3}{24\rho ch^2} + \frac{Eh}{\rho c}\sum_{n\,odd}\frac{4d^3}{n^4h^3\pi^4}\varepsilon^{-n^2h^2\pi^2t/d^2} \quad . \quad (8)$$

at $x = d$ $$\theta = \frac{1}{\rho c}\left(\frac{Ed}{2} + D\right)t + \frac{Ed^3}{24\rho ch^2} - \frac{Eh}{\rho c}\sum_{n\,odd}\frac{4d^3}{n^4h^3\pi^4}\varepsilon^{-n^2h^2\pi^2t/d^2} \quad . \quad (9)$$

For initial conditions let $p \to \infty$ in eqns. (6) and (7).

At $x = 0$ $$\theta \to \frac{D}{p\rho c}, \quad \text{i.e. } \theta = \frac{Dt}{\rho c} \quad . \quad . \quad . \quad . \quad (10)$$

at $x = d$, $$\theta \to \frac{Ed + D}{p\rho c}, \quad \text{i.e. } \theta = \frac{(Ed + D)t}{\rho c} \quad . \quad (11)$$

Thus the initial rate of rise of temperature is directly proportional to the energy generated at a point.

The slope after time t is obtained by differentiating eqns. (8) and (9) with respect to t, giving

at $x = 0$ $$\frac{d\theta}{dt} = \frac{1}{\rho c}\left(D + \frac{Ed}{2}\right) - \frac{E}{\rho c}\sum_{n\,odd}\frac{4d}{\pi^2n^2}\varepsilon^{-n^2h^2\pi^2t/d^2} \quad . \quad (12)$$

at $x = d$ $$\frac{d\theta}{dt} = \frac{1}{\rho c}\left(D + \frac{Ed}{2}\right) + \frac{E}{\rho c}\sum_{n\,odd}\frac{4d}{\pi^2n^2}\varepsilon^{-n^2h^2\pi^2t/d^2} \quad . \quad (13)$$

Subtraction of the initial slopes [eqns. (10) and (11)] gives the change of slope in time t, and hence the percentage change in slope.

Percentage change in slope at $x = 0$ is

$$\frac{Ed}{2D}\left(1 - \frac{8}{\pi^2}\sum_{n\,odd}\frac{\varepsilon^{-n^2t/T}}{n^2}\right) \times 100 \quad . \quad (14)$$

and at $x = d$ is

$$-\frac{Ed}{2(Ed + D)}\left(1 - \frac{8}{\pi^2}\sum_{n\,odd}\frac{\varepsilon^{-n^2t/T}}{n^2}\right) \times 100 \quad . \quad (15)$$

where $T = d^2/\pi^2h^2$

If it is assumed that the average slope measured over the time considered is the average of those at $t = 0$ and $t = t$, the percentage errors, ξ, become

at $x = 0$

$$\xi_0 = \frac{Ed}{4D}\left(1 - \frac{8}{\pi^2}\sum_{n\,odd}\frac{\varepsilon^{-n^2t/T}}{n^2}\right) \times 100 \quad . \quad . \quad . \quad (16)$$

at $x = d$

$$\xi_d = -\frac{Ed}{4(Ed + D)}\left(1 - \frac{8}{\pi^2}\sum_{n\,odd}\frac{\varepsilon^{-n^2t/T}}{n^2}\right) \times 100 \quad (17)$$

(10.2) Error due to Heat Loss to Surrounding Air

Fig. 8 shows a typical cooling and heating curve for a point in the core of a machine.

Let P be the rate at which energy is generated in a small volume around the point, W/cm^3.

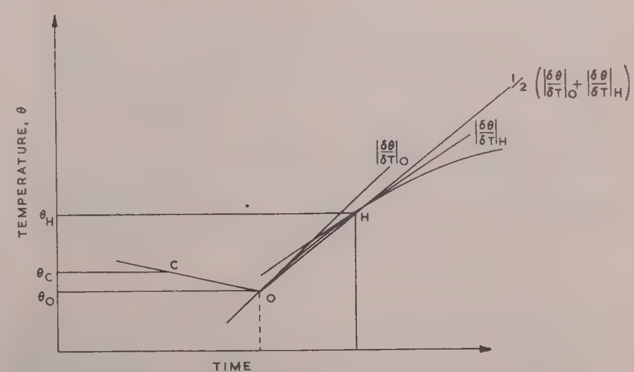

Fig. 8.—Typical cooling and heating curve.

Let θ_0 = Temperature at O when the field is applied, deg C.

θ_c = Temperature at C from which a cooling curve is taken, deg C.

θ_H = Temperature at H to which a heating curve is taken, deg C.

$\left|\frac{\partial\theta}{\partial t}\right|_0$ and $\left|\frac{\partial\theta}{\partial t}\right|_H$ = Slopes of the heating curves at O and H.

θ_a = Ambient temperature.

Assume that:

(*a*) The cooling obeys Newton's law of cooling, i.e. that rate of loss of heat is equal to k times the temperature difference above surroundings.

(*b*) The mean slope measured over the portion CO is the mean of the slopes of the cooling curve at C and O.

(*c*) The slope measured over the portion OH is the mean of the slopes of the heating curve at O and H.

Now
$$P = k(\theta_0 - \theta_a) + \gamma\frac{\partial\theta}{\partial t} \quad . \quad . \quad . \quad . \quad (18)$$

where k and γ are constants.

At H,
$$P = k(\theta_H - \theta_a) + \left|\frac{\partial\theta}{\partial t}\right|_H \quad . \quad . \quad . \quad . \quad (19)$$

At O,
$$P = k(\theta_0 - \theta_a) + \left|\frac{\partial\theta}{\partial t}\right|_0 \quad . \quad . \quad . \quad . \quad (20)$$

Measured slope of
$$\mathrm{OH} = \frac{\gamma}{2}\left(\left|\frac{\partial\theta}{\partial t}\right|_0 + \left|\frac{\partial\theta}{\partial t}\right|_H\right) \quad . \quad . \quad . \quad . \quad (21)$$

Measured slope of
$$\mathrm{CO} = \frac{k}{2}[(\theta_c - \theta_a) + (\theta_0 - \theta_a)] \quad . \quad . \quad (22)$$

$$P_{measured} = \frac{\gamma}{2}\left(\left|\frac{\partial\theta}{\partial t}\right|_0 + \left|\frac{\partial\theta}{\partial t}\right|_H\right) + \frac{k}{2}[(\theta_c - \theta_a) + (\theta_0 - \theta_a)] \quad (23)$$

Substituting for $\left|\frac{\partial\theta}{\partial t}\right|_0$ and $\left|\frac{\partial\theta}{\partial t}\right|_H$ from eqns. (19) and (20),

$$P_{measured} = P - \frac{k}{2}(\theta_H - \theta_c) \quad . \quad . \quad . \quad (24)$$

Under steady-state conditions, let the final temperature be θ_F.

Then
$$\left|\frac{\partial\theta}{\partial t}\right|_F = 0 \text{ and } P = k(\theta_F - \theta_a) \quad . \quad . \quad . \quad (25)$$

From eqn. (24),
$$\text{Error} = -\frac{k}{2}(\theta_H - \theta_c) \quad . \quad . \quad . \quad . \quad (26)$$

Hence
$$\text{Percentage error} = -\frac{(\theta_H - \theta_c)}{2(\theta_F - \theta_a)} \times 100 \quad . \quad . \quad (27)$$

The assumption that the measured slope is the mean of the extreme slopes is not strictly true, but for the small temperature rises considered the assumption results in only a small error.

Newton's law of cooling is not strictly applicable as the temperature in the body considered is not uniform. The result, however, gives an indication of the order of magnitude of the error to be expected.

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SOME NOTES ON THE ELECTRICAL REQUIREMENTS OF GENERAL CARGO DOCKS

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SUMMARY

The paper, which is written with special reference to the South Wales orts, outlines the function of a general cargo dock and the effect of rouping such docks into a port system; its object is the stimulation f thought and discussion on the improvement of electrical engineering n the dock industry—an industry where efficiency can affect the living andards of everyone in Britain.

The paper illustrates and comments on the electrical supply and istribution practices of the industry, the electrical features of the umping plant used for hydraulic power production and impounding ervices, the requirements for tenants and the safety of shipping, and discusses some of the problems associated with the mechanical-andling plant.

Passenger reception and handling peculiar to the liner terminal orts, and the electrical installations of ships, are outside the scope f the paper.

(1) INTRODUCTION

A dock provides a safe anchorage in conjunction with rail nd road heads having storage accommodation and mechanical acilities for efficient cargo handling. It may be an open dock ubject to tidal variation, an enclosed tidal dock requiring lock ntrances or an enclosed impounded dock requiring both lock ntrances and pumping plant to maintain its water level. Where uch docks exist in close proximity to one another, they have ended to form a port system, giving a stronger financial strucure, standardization of plant and flexibility to meet peak emands by the transfer of mobile plant, so enabling a profesional staff to be engaged continuously on development and naintenance and justifying capital investment on base workshop acilities.

(2) POWER SUPPLY AND DISTRIBUTION

The dock authority needs an electrical supply and distribution ystem for its quayside facilities and pumping stations, and is ften the electricity supply authority for the many tenants ccupying the extensive dock estates. These requirements necesitate 6·6 or 11 kV distribution. Private generating plant is not n economic solution, so that bulk supplies are obtained from he Electricity Boards and are invoiced on the published naximum-demand tariffs, having load-factor and power-factor eatures; no preferential treatment is offered, notwithstanding he elimination of Electricity Board capital in supplying to dock enants. Dock authorities usually offer their tenants such ublished tariffs, relying on maximum-demand diversities and he magnitude of the load to give a financial working margin. ig. 1 shows the electrical loading trend of five major ports andling 18–25 × 10^6 tons annually, and also the typical nonthly and daily demand curves of a port.

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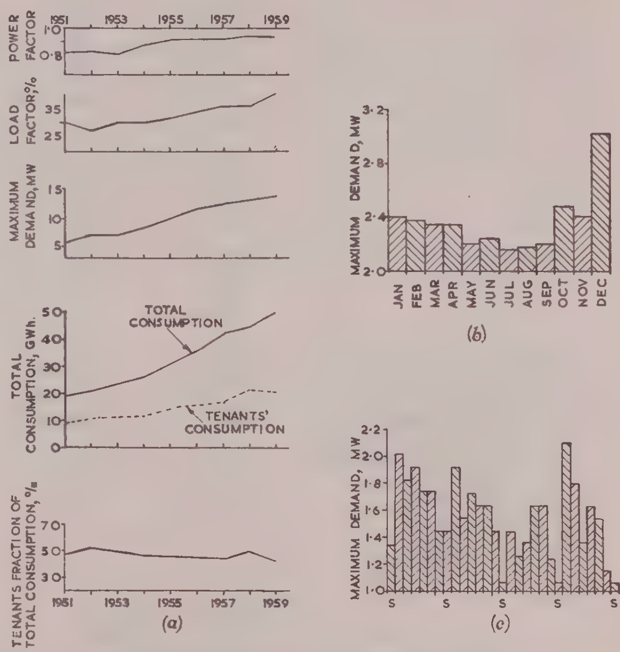

Fig. 1.—Electrical load at the South Wales docks.
(a) Growth of consumption, maximum demand, load and power factors.
(b) Monthly maximum demand.
(c) Daily maximum demand (S = Sunday).

(2.1) Factors Affecting the Design of the Electrical Distribution System

The designer of a dock electrical distribution system must understand that

(a) The load factor is lower than continuously working process plant, but supply is required 24 hours per day for seven days per week.

(b) Certain parts of the system are vital.

(c) The types of load and load factor of tenants and dock machinery vary widely, as exemplified in Table 1.

(d) Water crossings, sometimes preventing easy access to substations, will be met.

(e) There are intermittent but substantial demands at many places, necessitating a larger number of substations per 1 000 yd than usual, giving a high ratio of transformer capacity to system maximum demand.

(f) Cables must be used for the h.v. supply, but l.v. overhead distribution may be a satisfactory economic solution for some areas of the estate.

(2.2) Main Distribution System

Where dock electrical systems involve water passages, the distribution should be a ring, both physically and electrically. Fig. 2 shows a 6·6 kV application with pilot protection, and gives data for several installations. The hydraulic power

Table 1

TYPICAL DOCK LOADS

Installation	Installed capacity	Maximum demand	Load factor	Diversity factor	Remarks
	kW	kW	%	%	
Dry dock and ship repairing firms A	1 400	260	20	18·6	Workshop machinery; no pumping plant
B	440	100	20	22·7	
C	800	260	26	32·5	
D	2 400	350	18	14·6	
E	2 500	800	18	32	Workshop machinery; including pumping plant
F	1 000	300	15	30	
G	1 500	600	12	40	
F and G (summated)	2 500	830	14	33·3	
H	650	140	16	21·6	Repairs afloat
I	260	75	12	28·9	
Board mills	3 150	1 500	45	48	From dock network; restricted peak period m.d.
		500		16	From consumer's generator set
Flour mills A	1 100	708	61	64	Mill only
B	2 400	1 350	43	56	Mill with pneumatic intake plant
C	3 800	2 023	36	53	
D	3 195	1 750	43	55	Mill with pneumatic intake plant and provender section
Cold stores	806	218	58	27	
Briquette works ..	2 000	935	54	46·5	
Sawmills A	380	38	10	10	
B	361	114	14	32	
C	162	51	5	32	
Oil-tank farm	176	80	16	45	
Hydraulic pressure pumping station	1 008	655	54	65	
Impounding station ..	900	578	21	64	Also used for dry-dock pumping
Combined hydraulic and impounding station	1 280	1 061	13	83	
Dry-dock pumps ..	478	478	3	100	

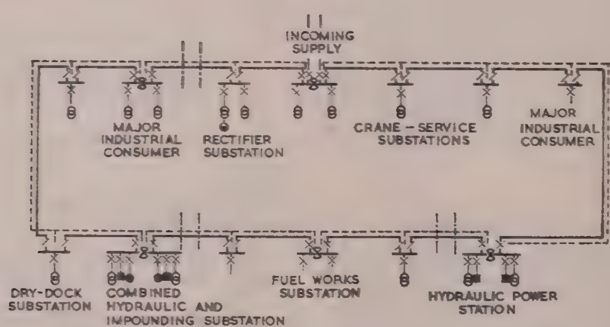

Fig. 2.—Typical dock ring-main system.

/ Wing isolator.
8 Bus switch.
× Circuit-breaker.
8 Transformer.
Rectifier.
Hydraulic pump.
Impounding pump.
Water crossing.

Intake voltage	Length	Transformer rating		Design rating	Transformer power / Design power	Number of substations	Substations per 1 000 yd	Protection
		415-volt	3·3 kV					
kV	yd $\times 10^3$	MVA	MVA	MVA				
11	8·1	8·25	6	5·5	2·6	13	1·62	Inverse time
6·6	3·1	2·5	1	3·75	0·93	5	1·62	Inverse time
6·6	6·25	3·5	5	5·5	0·73	13	2·07	Pilot
11	8·35	4·5	5	5·5	1·7	15	1·8	Pilot
11	6·2	2·35	3·4	3·9	1·4	6	0·97	Inverse time

tation and a tenant operating a continuous process are considered as the points for discriminatory protection. Craneage uays and minor substations are supplied via ring-main oil ircuit-breakers and isolator units, with single transformers and hunt-trip fuse protection. Failures of such equipment are so ew that the extra cost of transformer duplication and discriminating protection for such substations is unjustified. For maller dock systems with a fault clearance time of at least sec, inverse-time protection is satisfactory.

(2.3) Substations, Switchgear and Transformers

The materials and methods of general supply and distribution ractice are satisfactory; 250 MVA at 11 or 6·6 kV usually meets he fault ratings of the supply, and air-insulated busbar h.v. witchgear of the vertical-isolation horizontal-draw-out type is uitable. The merit of air-insulated in comparison with compound-filled busbars is supported by the excellent record of ir-insulated truck-type horizontal isolation gear installed from 920 onwards.

Experience with modern equipment confirms this view. Oil ircuit-breakers for lines of above 300 amp capacity and h.r.c. uses for circuits below 300 amp give satisfaction on the lowoltage services. Transformers should be suitable for either ndoor or outdoor service, and the number of sizes should be ept to a minimum; with an 11 kV supply, 250, 500 and 750 kVA ransformers with 433-volt secondaries are customary for dockde distribution, and 2 MVA units with 3·3 kV secondaries for umping stations. Buchholtz relays or winding-temperatureecording devices are unnecessary, but silica gel breathers are sual on all transformers and conservators on those above 50 kVA at 11 kV.

(2.4) Cables

The numerous underground services, old foundations and various types of filling encountered, together with the many rail, road and water crossings, make cable-laying conditions difficult. Single-wire-armoured and served cables are used for the general laying, and double-wired-armoured non-draining types having double servings are used for the water crossings.

Direct laying in sand with cover tiles at a depth of 2 ft is generally satisfactory; for road crossings, conduit surrounded with concrete laid at a clear depth of 3 ft is acceptable, but for rail crossings the depth is increased to 4 ft and the conduits are extended at least 4 ft from the edge of the nearest rail to avoid damage from the hammering of passing rail traffic.

At water crossings, either a culvert or a wall chase is provided at the construction stage. The culvert is best for installation and maintenance, but its first cost is high and the chase is more often met. Separate chases should be used for the electrical services, to prevent cable damage and supply interruptions due to repairs of other services.

The cable should be cleated down the vertical chase at about 3 ft intervals and dressed down into a horizontal chase, to avoid disturbance when sluicing, emptying or filling the locks. To avoid joint strain, a clamping anchorage is necessary at each side of a water crossing when neither culvert nor chase is provided.

(2.5) Power Factor

With the transformer capacities, maximum demand and load factors indicated in Figs. 1 and 2, and with the tenants' power factors under penalty at not less than 0·85 lagging, the overall dock power factor can be maintained between 0·92 and 0·97

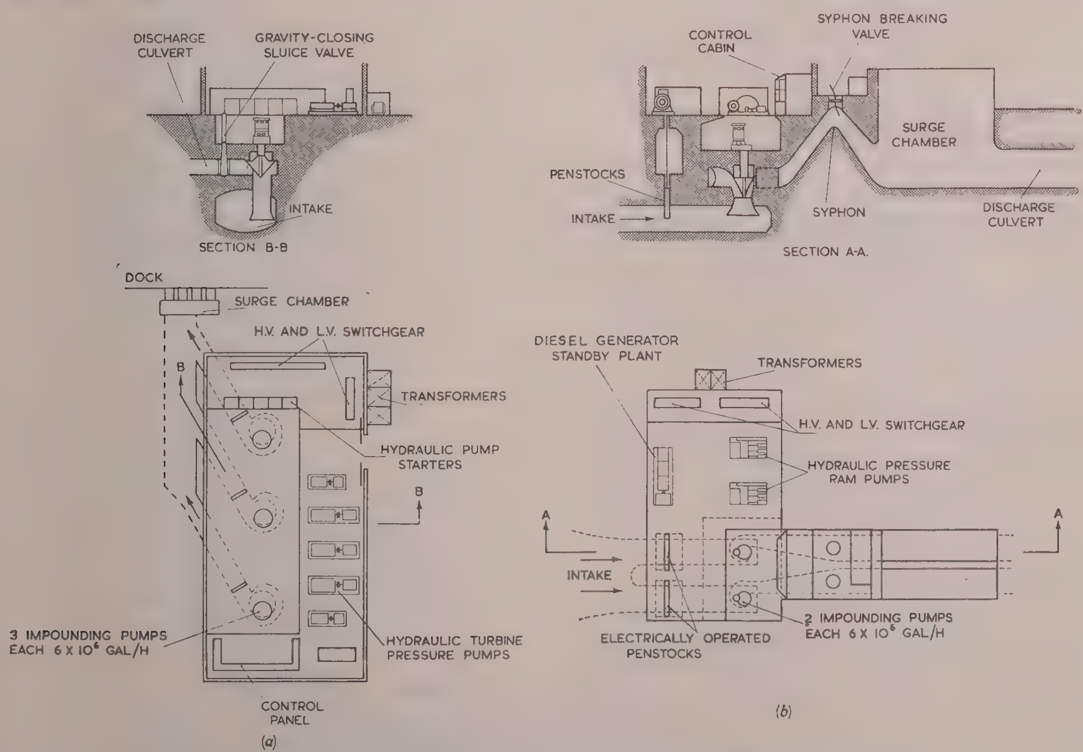

Fig. 3.—Arrangements of typical hydraulic and impounding pumping stations.
(a) Station converted from steam to electric operation.
(b) Station of new design.

lagging by connecting, without automatic control, capacitance of approximately 5% less than the total connected transformer reactance.

(3) ELECTRICAL REQUIREMENTS OF PUMPING PLANT

In addition to those required for dry-dock operation, pumps are needed for impounding and hydraulic power services; for minimum operating and capital costs, if both types are required, they should be in one station.

Fig. 3 shows the conversion of a steam-operated station having design restrictions of the existing impounding suction and delivery culverts which necessitate the use of a gravity-closing sluice valve and providing hydraulic power for general dock use. It also illustrates a new installation of advanced hydraulic design, the gravity-closing sluice valve being replaced by a syphon; small electrically driven hydraulic-pressure ram pumps of sufficient capacity for the lock equipment only are provided. Operation of the lock machinery and lighting is safeguarded against mains failure by the automatic starting of a Diesel-driven generator set.

(3.1) Impounding Pumping Plant

(3.1.1) Motors.

An impounding pump motor runs for about six hours against a varying hydraulic head, and the mixed-flow propeller pump now in general use enables a constant-speed motor to be used efficiently for this duty. Drip-proof slip-ring induction motors were used for many years, but the capacity of modern distribution systems enables on-line squirrel-cage motors of about 700 h.p. at 3·3 or 6·6 kV running up to speed in 4 sec with starting currents of 3–3½ times full load to be switched direct by an oil circuit-breaker, so reducing both capital and maintenance costs. A trend towards the use of vertical pumps has been noted, necessitating vertical motors with ball and roller bearings. Results obtained with this construction have been satisfactory.

The design speed of impounding pumps for from 6 to 3½ × 10^6 gal/hour ranges from 120 to 340 r.p.m. With the higher-speed pump the motor may be of the direct-coupled low-speed type, but for the low-speed machine a motor speed of 750 r.p.m. driving the pump through a reduction gearbox is the economic solution. Continuously rated machines are used, thus enabling overloads to be accepted safely for part of the duty cycle (Fig. 4) if design conditions, test results or changing dock demands make it necessary.

Motor protection is by means of thermal-overload relays with phase-failure, overload, short-circuit and earth-fault features.

If the overload conditions exceed those permissible, the difficulty may be overcome effectively by using a slip-ring induction motor driving through a scoop-controlled fluid coupling, so arranged that, as the induction motor reaches its full load under the varying head conditions, the scoop is operated by a torque motor to reduce the speed and output of the pump. Maintenance troubles with such a scheme operated since 1936 have been few.

(3.1.2) Switchgear.

Direct switching of the impounding pump motors by remote control of solenoid-operated 3·3 or 6·6 kV vertical-isolation horizontal-drawout oil circuit-breakers has proved satisfactory. Examination of the circuit-breaker contacts after 1 000 operations has indicated no signs of burning, but carbonization of the oil occurs, necessitating an oil change after about 250 operations.

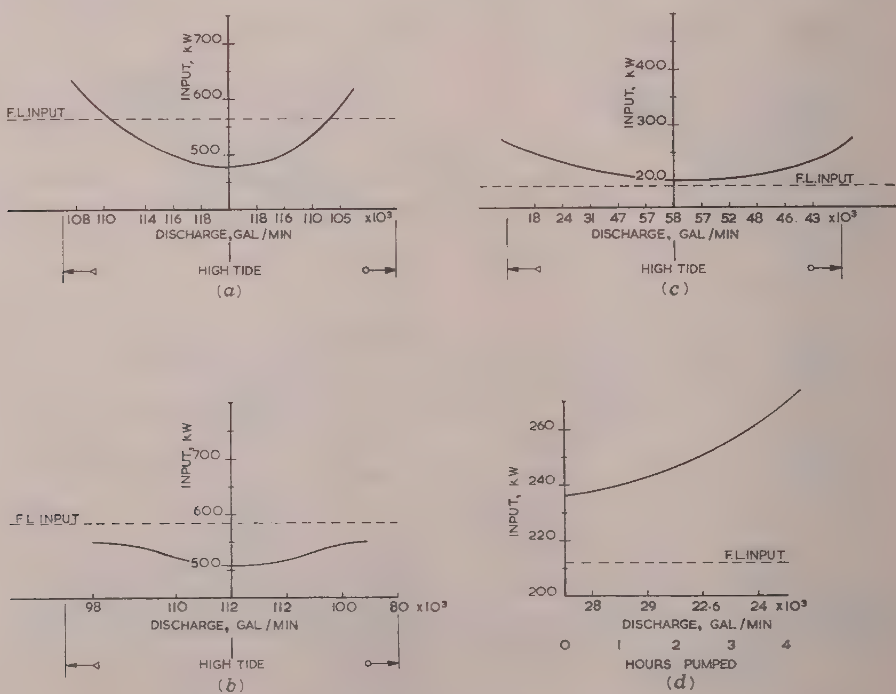

Fig. 4.—Characteristics of impounding pumps.

(*a*) High-speed mixed-flow pump; maximum stator temperature = 50°C, ambient temperature = 18°C.
(*b*) Low-speed mixed-flow pump, maximum stator temperature = 57·5°C, ambient temperature = 16°C.
(*c*) Axial-flow pump; maximum stator temperature = 69·5°C, ambient temperature = 13°C.
(*d*) Centrifugal pump; maximum stator temperature = 67°C, ambient temperature = 20·5°C.
The arrows denote the periods 3 hours before and after high tide.

(3.2) Hydraulic Pressure Plant

Hydraulic pressure supply requires pumping speeds of between 50 and 800 gal/min at 850 lb/in^2. Ram pumps can be used up to 250 gal/min, but the turbine pumps are preferable above this. Automatically controlled low-head pumps supply water to the suction tank where return-water systems do not exist.

(3.2.1) Motors.

The application of turbine pumps to hydraulic pressure duty necessitates direct coupling of the pump and motor. High-voltage slip-ring motors of up to about 650 b.h.p. at 1 460 r.p.m. have been the accepted practice, but recent developments require smaller turbine pumps running at 2 950 r.p.m. for greater flexibility of control, higher efficiency and minimum cost; 2 950 r.p.m. slip-ring machines are in use, but continuously rated squirrel-cage machines should be used if supply conditions permit, thus eliminating brush and slip-ring maintenance. Experience with squirrel-cage machines has been excellent, but the fluctuating hydraulic demand necessitates frequent starting and stopping, and critical design conditions may be met.

Low-lift pump motors are 10–30 h.p. 750 r.p.m. squirrel-cage machines and pose no problems. Past practice for ram-pump drives has been continuously running slip-ring motors, with the water by-passed by a hydraulic-accumulator-actuated solenoid-operated diverting valve at times of low demand. Recent developments have been the use of direct-on-line-started squirrel-cage motors driving through scoop-controlled fluid couplings, giving light-load starting conditions and pump-speed control from the accumulator position. Under no-load conditions the light-running motor closes down after a period governed by a relay setting.

(3.2.2) Control and Protection.

Two methods of control have been developed for the electrically driven turbine pumps applied to hydraulic pressure supply. One method uses the movement of the accumulator to switch the pump on and off, but has the inherent disadvantage of possibly shutting off the pump when a heavy hydraulic demand might require its immediate restarting. The other well-proved method uses the water demand as the controlling feature, suitable relays being actuated by the head variation caused by the flow through the suction-main Venturi meter. This method failed when attempted in 1925: sudden surges in the main caused the slip-ring pump motors to start up one after the other before the previous motor in the sequence reached pumping speed, the hunting being aggravated by the large pump sizes then in use.

With the use of high-speed squirrel-cage machines necessitating limitations of the starting duty to about 12 starts an hour, the control scheme has been developed to take advantage of the pump's characteristic quantity/pressure variation used in conjunction with a hydraulic accumulator. An incoming relay with a delay setting of between 0 and 60 sec is incorporated to prevent starting on a momentary surge, and a retaining relay with a setting of between 5 and 10 min ensures that, when started, a pump will continue to run for the predetermined period, whatever the demand. The sequence and protection for such a hydraulic pumping installation are shown diagrammatically in Fig. 5.

(3.2.3) Switchgear.

The frequent starting and stopping of the hydraulic pump motors necessitates control by both oil circuit-breakers and air-break stator contactors. Oil circuit-breakers, fitted with under-voltage release, arranged for remote solenoid closing and d.c. shunt tripping and having combined thermal and attracted-armature relays to protect against overload, single-phasing, earth faults and short-circuits, control the supply, and air-break stator contactors with h.r.c. back-up protection, interlocked with their respective circuit-breakers, perform the operating duty. No maintenance troubles have been experienced with this arrangement during several thousands of operations.

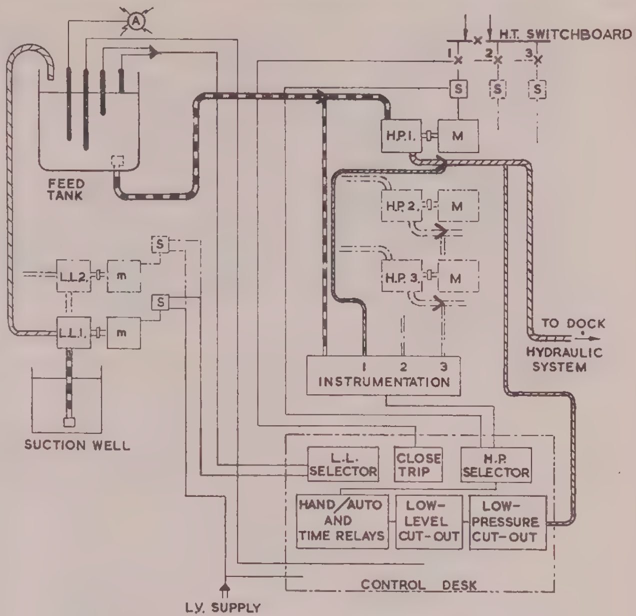

Fig. 5.—Operating sequence in hydraulic pumping station.

L.L. Low-lift pump.
H.P. Hydraulic pump.
M. High-voltage motor.
m. Low-voltage motor.
S. Direct-on-line starter.
Suction.
Delivery.
Venturi.
Low-level alarm.

(4) LOCK ENTRANCE MACHINERY

Lock-gate operation by hydraulically powered winch-and-chain gear for entrances less than 60 ft wide and hydraulic piston mechanisms for 60–100 ft entrances are usual, but the application of a squirrel-cage motor driving through a fluid coupling is satisfactory for chain-operated gates.

Attempts to apply an electric drive to the piston-and-connecting-rod system, although a more difficult problem, have been reasonably successful. One solution is to supply each gate machine by a variable-speed pump unit driven by an electric motor. An alternative method is to use a rack, actuated linearly by a pinion, driven electrically through a fluid coupling.

Hydraulic-powered gate machines fed by a hydraulic pipe ring around the lock give reliable service, which, if supplied by an electrically operated pumping plant with Diesel-driven standby situated on the lock side, is an economic method and worthy of retention.

(5) ELECTRICITY APPLIED TO THE MECHANICAL-HANDLING PROBLEMS

The mechanical-handling process involves the efficient unloading and loading of general cargo items of all shapes and weighing from a few hundredweights to many tons or bulk cargo, e.g. iron ore. With the exception of coal loading, where the hydraulically powered hoist forms a relatively efficient unit, modern practice is the use of electrical appliances; indeed, even for coal loading, electrically operated conveyors are beginning to replace the hydraulic hoists.

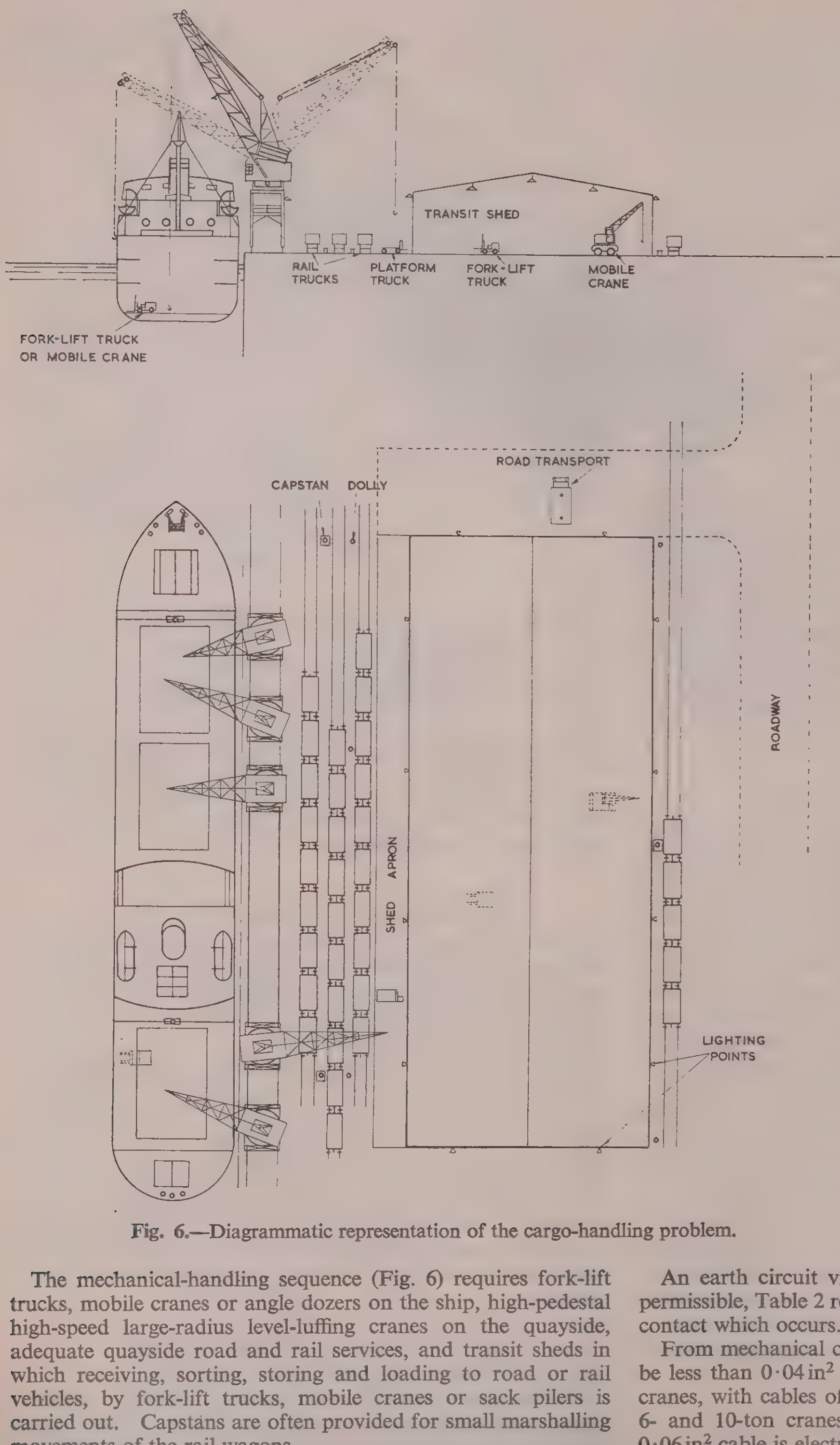

Fig. 6.—Diagrammatic representation of the cargo-handling problem.

The mechanical-handling sequence (Fig. 6) requires fork-lift trucks, mobile cranes or angle dozers on the ship, high-pedestal high-speed large-radius level-luffing cranes on the quayside, adequate quayside road and rail services, and transit sheds in which receiving, sorting, storing and loading to road or rail vehicles, by fork-lift trucks, mobile cranes or sack pilers is carried out. Capstans are often provided for small marshalling movements of the rail wagons.

Bulk cargo discharge utilizes 4-line 10-ton-capacity grabbing cranes discharging to rail wagons; but if site conditions permit direct discharge to the user, specialized berths having ore unloaders or kangaroo cranes feeding by conveyor to the stock-yards are adopted.

(5.1) Electrical Distribution of Crane Quays

Although many docks retain the d.c. supplies of past practice and notwithstanding the advantages of d.c. crane control, economics preclude d.c. distribution for new quays and a.c. supplies have been established for many years. It is thus intended to discuss a.c. supplies only. Crane-quay electrical loading conditions depend greatly upon the type of cargo being handled at any given time, but the tests (Fig. 7) taken on general-cargo and grabbing-crane berths handling a variety of different types of cargo give an assessment upon which crane-quay distribution may be safely based.

Sufficient security of primary supply for the crane quays is obtained from the h.v. mains by supplying via ring-main units, but the cables feeding the water-tight plug boxes, which supply the cranes via flexible trailing cables, should be in duplicate at least, with the plug boxes arranged alternately on the cables (Fig. 8). Paper-insulated lead-covered steel-wire-armoured cables are used for this duty, and laying through ducts is necessary with built-up quays. In new quay construction a chase covered with removable concrete slabs, giving greater carrying capacity for given cable sizes and ease of installation or replacement, has much to commend it. If removable reinforced-concrete slabs laid on sand are used for the quay surface, direct laying in the ground is satisfactory.

Craneage requires a 415-volt 3-phase 3-wire supply, lighting and heating services usually being provided by a transformer installed on the crane to give a 110-volt output with the centre point earthed. Although the requirement is a 3-wire supply only, $3\frac{1}{2}$-core cable is to be preferred for the distribution system. The half-core gives the main earth continuity back to the substation independently of the lead and armouring of the cables, and such an arrangement with $3\frac{1}{2}$-core t.r.s. flexible cable between the plug box and the crane gives loop impedances between 0·006 and 0·26 ohm for little increase in cost.

An earth circuit via the crane wheels and rail track is not permissible, Table 2 recording the wide variation of rail-to-wheel contact which occurs.

From mechanical consideration the flexible cables should not be less than 0·04 in^2 in section; this size is applicable to 3-ton cranes, with cables of up to 0·2 in^2 or more being used for the 6- and 10-ton cranes. Tests (Fig. 9) indicate, however, that 0·06 in^2 cable is electrically adequate for the latter sizes.

(5.1.1) Crane Plug-Boxes.

The plug-box market is relatively small, making standardization on one size desirable, and the 200 amp continuous rating is adequate. Apart from the trailing cable, plug boxes are the

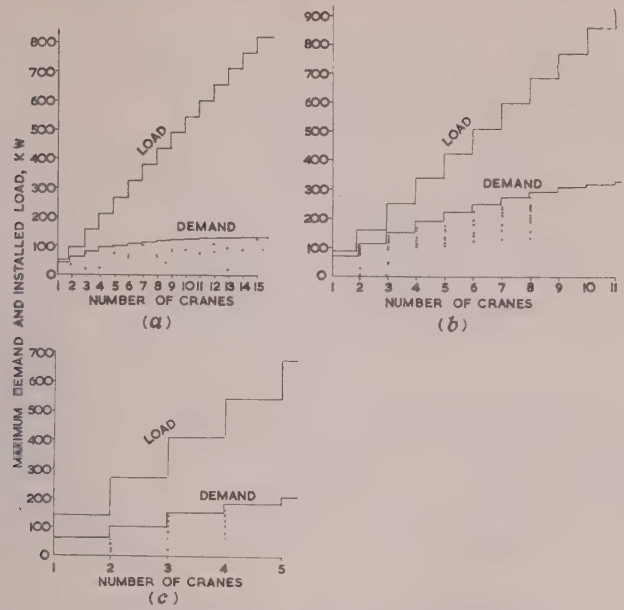

Fig. 7.—Electrical loading of quayside cranes.

Fig.	Crane		Motor power			Commodity	Consumption	
	Capacity	Control	Hoist	Luff	Slew		Variation	Average
	Tons		h.p.	h.p.	h.p.		kWh/ton	kWh/ton
(a)	3 and 6/3	Rotor resistance	55	12	6	General cargo	0·2–1·4	0·43
(b)	6/3	Opposed torque	85	17·5	12	General cargo	1·2–5·1	2·55
(c)	10	Counter current	2×75	20	15	Iron ore	0·3–0·5	0·41

Table 2

CONTACT RESISTANCE BETWEEN CRANE WHEEL AND RAIL

Crane as found	Crane after moving	Variation in resistance	
		Increase	Decrease
ohms	ohms		
0·1	0·2	2 : 1	
0·1	11·5	115 : 1	
0·15	20·5	134 : 1	
0·25	14·0	56 : 1	
0·4	75·0	188 : 1	
0·6	19·0	32 : 1	
0·65	3·25	5 : 1	
0·9	0·2		5 : 1
1·25	25·0	20 : 1	
1·65	7·0	4 : 1	
1·85	0·9		2 : 1
2·1	4·5	2 : 1	
4·0	8·75	2 : 1	
4·5	10·0	2 : 1	
8·0	1·15		7 : 1
8·4	30·0	4 : 1	
9·0	0·35		26 : 1
12·5	1·55		8 : 1
13·0	30·0	2 : 1	
35·0	7·0		5 : 1

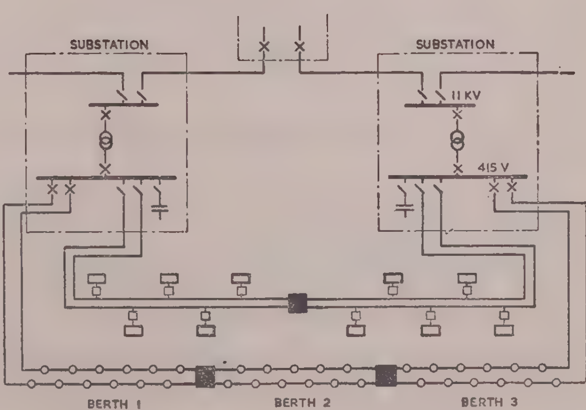

Fig. 8.—Electricity supply to quayside capstans and crane plugs.

■ Selective link box.
○ Crane plug-box.
□ Emergency kick switch.
▭ Capstan.

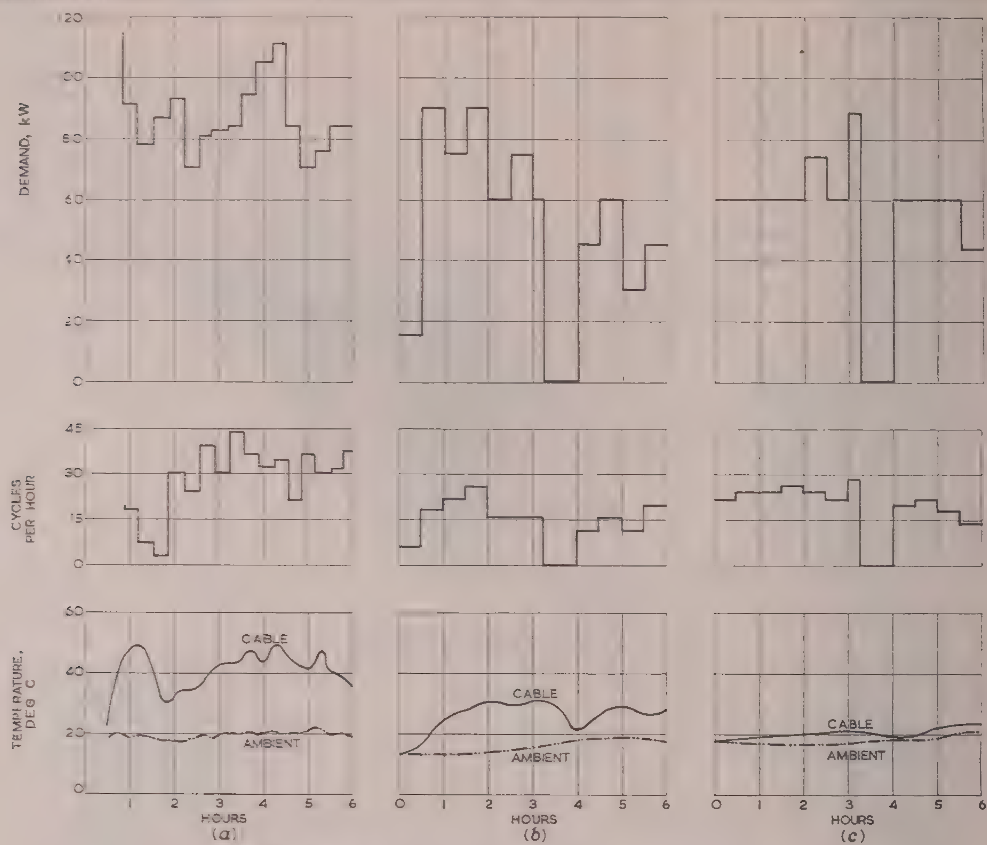

Fig. 9.—Working temperatures of 4-core t.r.s. trailing cables.

Fig.	Crane capacity	Cable size	Hoist power	Type of control
	tons	in²	h.p.	
(a)	6/3	0·06	85	Opposed torque
(b)	10	0·06	2 × 75	Counter current
(c)	10	0·2	2 × 75	Counter current

most vulnerable part of the crane supply system, for, in addition to normal weather hazards, quayside flooding by ship discharge—although forbidden—does sometimes happen. Experience dictates that a satisfactory plug box (Fig. 10) should

(a) Be contained in a robust waterproof box, preferably of cast iron.

(b) Have the switchplug interlocked to prevent insertion or withdrawal of the plug from the socket unless the switch is in the off position.

(c) Have a robust switch-and-socket mechanism able to work for long periods without attention, but capable of easy replacement and isolation in the case of fault.

(d) Have plug sockets shrouded and watertight, to prevent short-circuits across the contacts with the plug box submerged.

(e) Have the interior of the cast-iron box treated with anti-condensation paint and the top cover sealed with a watertight joint.

(f) Not have fuses to protect the trailing cable in the plug box or plug top; the inconvenience and cost of replacing such h.r.c. fuses on their occasional failure are greater than the short delay of circuit-breaker reinstatement.

Few existing designs fulfil these requirements.

(5.2) Balanced-Jib Level-Luffing Cargo and Grabbing Cranes

Electrically operated balanced-jib level-luffing cranes of relatively small radius and low pedestal structure, with hoisting speeds of 200 ft/min for 3-ton loads and 100 ft/min for 6-ton loads, have been the accepted practice for general cargo handling, using approximately four 3-ton cranes to one 6/3-ton crane per berth, the latter having a 2 : 1 mechanical change gear. The increasing size of general cargo vessel, the demands for a quicker turn-round and the trend for a greater proportion of cargo packages to exceed 3 tons are leading to the specification of 65/80 ft-radius 6/3-ton cranes, or possibly higher capacity where grabbing work predominates, on high pedestals, with hoisting speeds ranging between 150 and 300 ft/min and the mechanical speed-change gear replaced by automatic electrical methods. Maximum slewing and luffing speeds of 600 ft/min at maximum radius and 200 ft/min, respectively, remain approximately the same as the earlier designs.

Recordings of normal crane operating cycles for various types and capacities of crane are given in Fig. 11, which, if considered in conjunction with Fig. 7 and a specified number of cycles per hour, can give a reasonably accurate assessment of requirements.

(5.2.1) Crane Control.

The characteristics of the d.c. motor are excellent, but to obtain the advantages of a.c. distribution, cranes driven by slip-ring induction motors—notwithstanding the difficult speed

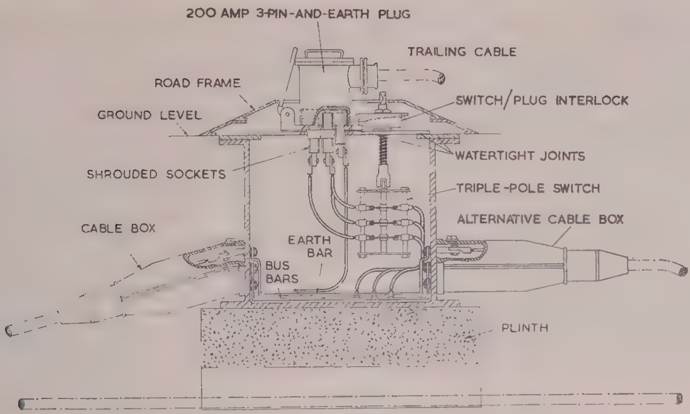

Fig. 10.—Typical watertight ground socket and plug.

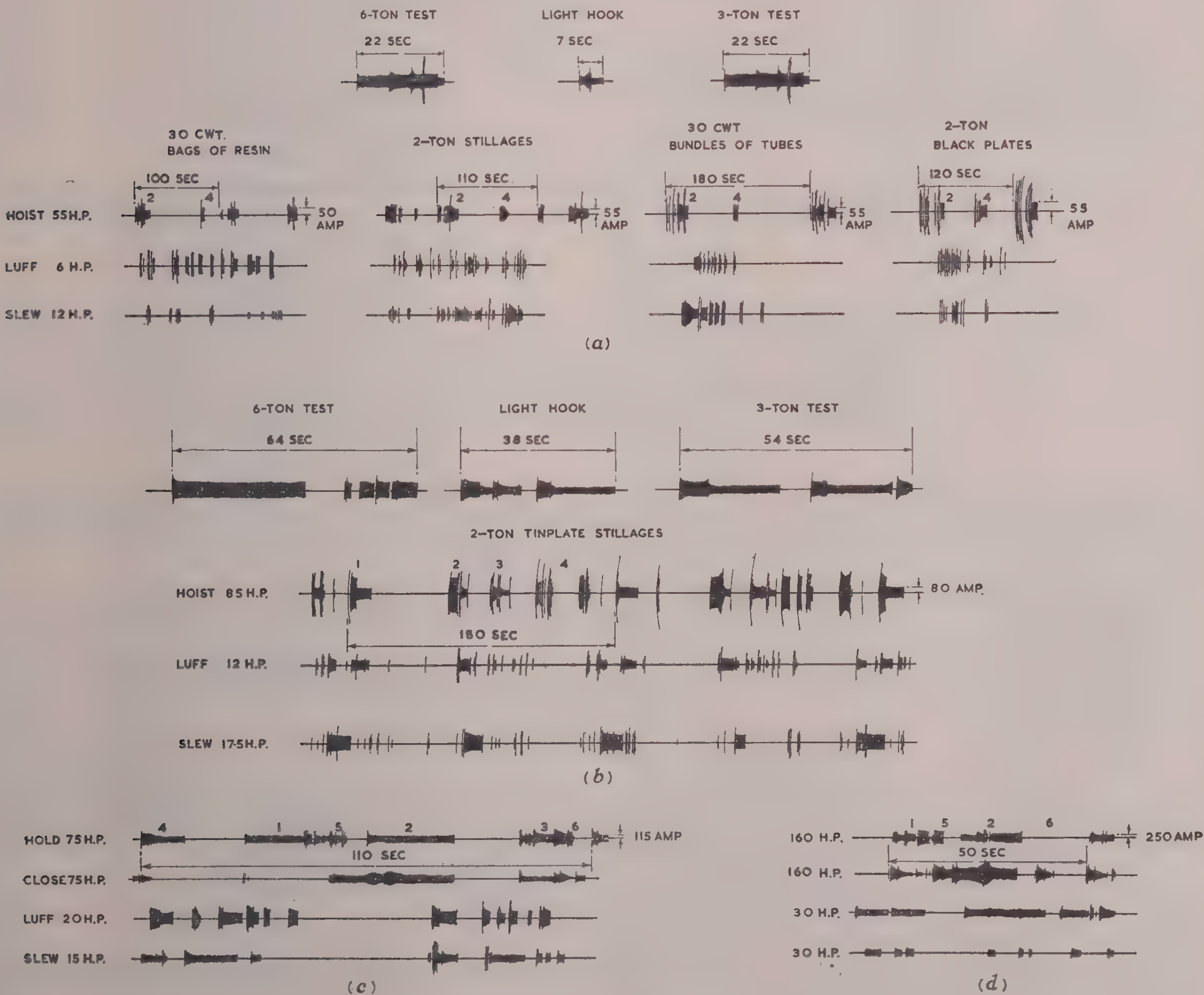

Fig. 11.—Crane operating cycles.

(a) Mechanical-gear-change 6/3-ton crane with a.c. rotor-resistance control and free-barrel lowering.
(b) 6/3-ton crane with opposed-torque control.
(c) 10-ton grabbing crane with counter-current control.
(d) 10-ton kangaroo crane with counter-current control.

1. Lower light.
2. Hoist loaded.
3. Lower loaded.
4. Hoist light.
5. Close grab.
6. Open grab.

control—are in general use. Depending upon the crane size and duty, drum resistance controllers of the full-load or the master-controller type operating in conjunction with contactors are suitable for slewing and luffing. For travelling, the full-load drum controller is adequate, but direct-on-line-starting squirrel-cage machines driving through fluid couplings has been recently applied.

The full-load drum controller, giving rotor-resistance speed control, together with a mechanical gear change on dual-purpose hoist motions, has given good service, but the higher speeds, greater size and increased mechanical efficiency of to-day necessitate electrical and physical improvement of control. The larger currents handled demand master-controller and contactor gear, and the higher speeds and size require improved braking, suitable low speeds to avoid snatch when tightening cargo slings, and the replacement of mechanical speed-changing gear by efficient electrical methods.

Various control schemes are compared in Table 3 and some speed/torque characteristics given in Fig. 12.

(5.2.2) Future Crane-Control Development.

The complicated methods developed to provide accurate speed control of a.c. slip-ring machines necessitates a recasting of lines of thought with a view to simplification. Resistance control of a.c. motors for slew and luff motions probably will be retained, and it is upon the hoisting motion that research must be concentrated.

The rapid development of semiconductor rectifiers directs

Table 3

COMPARISON OF CRANE CONTROL SCHEMES

	Method	Relative cost	Principle	Advantages	Disadvantages	Remarks
		%				
1.	Slip-ring motor with external resistance control	100 (=£900)	Variation of torque and speed by external resistance in rotor circuit	Minimum first cost	Steep torque/speed curves give rough operating conditions Speed control limited to below synchronism for hoisting and above synchronism for lowering	Low-speed small-radius cranes
2.	D.C. injection braking	158	Hoisting as for (1) Braking obtained by injecting direct current into rotor	Controlled lowering of loads	Steep hoisting torque/speed curves A wide range of low-torque operating speeds is not easily obtainable Does not provide for power lowering of light hook at predetermined low speeds	Operating conditions not satisfactory for high-speed general-cargo cranes
3.	Electro-mechanical	200	Low speeds obtained by partial application of an electro-mechanical brake automatically controlled by speed of motor		Speed controlled by mechanical dissipation of heat, giving high brake maintenance	Not suited to heavy-duty high-speed cargo cranes
4.	Counter-current braking	148	Control of lowering by having motor polarity as for hoisting with external resistance in circuit	Controlled lowering of loads with reduction in mechanical brake wear	Steep hoisting speed/torque curves giving rough operating conditions Hoisting speed control possible only with load speed below synchronism Does not provide for power lowering of light hook at predetermined low speeds	In this simple form is not suitable for dockside cranes
5.	Counter-current braking and relay control of rotor resistance	170	As (4), but with rotor resistance controlled by 'fluttering' relays	Flat torque/speed throughout lowering with 20% or more load. Single slow-speed hoist up to 60% load in addition to slip-ring-motor characteristics	As (4), but improved control	Suitable for grabbing cranes, where crane is always loaded with grab
6.	Counter - current - plus - relay control of rotor resistance plus pony motor	217	As (5), but using additional motor at 10–20% of hoist motor b.h.p.	As (5), with extension of the lowering characteristics over complete load ange	As (5), but additional maintenance of motor and control	Suitable for high-speed cargo cranes if space permits
7.	Counter - current - plus - relay control of rotor resistance plus two hoist motors	217	As (4), but using two motors, one of 75% normal hoist motor b.h.p.	Controlled lowering: mechanical change-speed gear not required: hoists light loads automatically at double rated speed	Steep hoisting speed/torque curve: additional rotating machine and space limitations	Advantageous for dual-speed cargo cranes where a considerable proportion of loads are below 25%
8.	Opposed torque using braking unit directly coupled to driving motor	312	Squirrel-cage-motor electric braking unit with field energized by a variable d.c. supply direct coupled to the hoist motor giving opposed torques	Smooth control from 10% to synchronous speed in both directions	Overall length can be difficult	Possible use on high-speed general-cargo cranes if space limitations permit: pole changing could be incorporated in this system to give high half-load speeds
9.	Opposed torque using two electrically separate windings in the same motor frame, forming braking and motoring units	432	By varying resistance of the motor system and the resistance and/or d.c. excitation of the brake system, the combination of opposing torques give different torque/speed characteristics for each system, providing stable no-load speeds where they cross	Stable speeds from standstill to full speed at all loads in both directions: electric braking to standstill: emergency footbrake unnecessary: twice full-load speed provided automatically for loads up to half full-load by pole changing	Heavy current peaks, low power factor and high electrical losses in the opposed-torque steps: motor requires forced ventilation: complicated contactor equipment	Suitable for high-speed general-cargo cranes where average lift is half-load or higher
10.	A.C. commutator motor	487	Speed control by moving of brushes	Smooth control from 10% to full speed	Commutator and brush-ring maintenance high: high rotor inertia: difficult for local repairs	Has proved very successful for fitting-out berths where duty is not arduous. Not favoured for general-cargo duty cycles
11.	Ward Leonard ..	305	A.C. motor driving d.c. generator	Excellent smooth control with small current peaks: high-speed light hook, with speeds automatically adjusting to load being lifted: no complication in contactor maintenance	Three machines, two with commutators to maintain.	Very suitable for high-speed crane operations

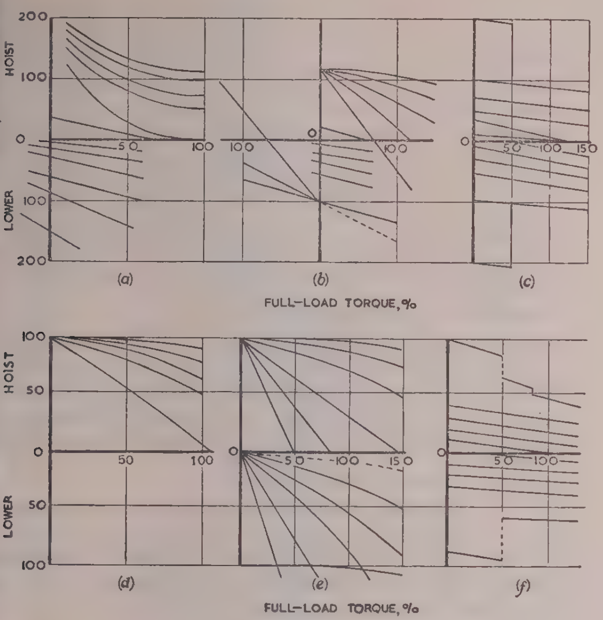

Fig. 12.—Crane hoist-motion characteristics.

Control
(*a*) D.C. potentiometer.
(*b*) Counter current.
(*c*) Opposed torque.
(*d*) A.C. rotor resistance.
(*e*) Rotor control with d.c. injection braking.
(*f*) Ward Leonard.

ttention to the d.c. motor with voltage divider control, supplied y a semiconductor rectifier, and the a.c. slip-ring motor with peed control effected by two semiconductor rectifiers, one rranged as a controlled inverter feeding the slip energy back nto the supply.

5.2.3) Crane Motors.

Dockside crane motors normally are supplied to the British tandard for drip-proof-enclosure 1-hour-rated slip-ring class-A-nsulated machines, with a thermometer-method temperature ise not exceeding 55° C. The crane builder, often working with evere limitations on tail radius and machinery-house width, elects a short large-diameter motor, and the motor manufacturer, n order to make a better-ventilated motor at minimum cost, ffers such high-inertia machines without considering the effect n crane performance. When loads moving at high speed ecome caught in the vessel or cargo, the hoist-mechanism inertia, f which the motor rotor provides a high proportion, puts severe tresses into the crane structure. Records indicate several jib ailures on the relatively slow older type of crane which must be ttributed to this cause, and the increasing speeds of present ractice places greater emphasis on this point. Temperature ecords taken under normal operating conditions show tempera-ure rises well within British Standard limits (Table 4), and this, ogether with recordings of operating conditions (Fig. 11), uggests that a more factual approach to the design of an dequately rated low-inertia motor for dockside crane duty is esirable.

5.2.4) Crane Faults and Failures.

An analysis of a year's fault records of 34 cranes of 3 and tons capacity handling general cargo, together with six 4-line 0-ton grabbing cranes and five 10-ton kangaroo cranes engaged n grabbing iron ore, is given in Table 5, classified according to the type of crane and delay times given per 1 000 tons of cargo handled per crane.

Table 4

Hoist Motor Temperature Records
(Approximately 30 cycles per hour for 7-hour period)

Crane	Ambient temperature	Maximum stator temperature by thermometer	Cargo
	deg C	deg C	
1	14·5	20	General
2	14·5	21	Phosphate
3	12	22·5	Iron ore
4	28	34·5	General
5	15·5	30·5	General
6	20	38	General
7	27	30·5	General

B.S. Class-A insulation; permitted temperature rise, 55°C.

Table 5

Crane-Fault Analysis for 12-Month Period

Electrical faults	34 cargo cranes, 3 and 6 tons capacity	Six grabbing cranes, 10 tons capacity	Five kangaroo cranes, 10 tons capacity
	Delay per crane	Delay per crane	Delay per crane
	min	min	min
Ground plugs ..	6·77	12·1	—
Trailing cables ..	9·27	12·91	—
Trips and relays ..	15·96	39·5	26
Controllers	2·82	5·25	6
Wiring..	5·43	4·66	36
Clutch or thrustors ..	10·81	9·09	—
Control panels ..	4·56	11·67	63
Supply failure ..	14·18	20·01	72
Motor faults	2·03	3·34	26
Brakes	1·82	3·0	8
Limit switches ..	—	—	41
Overhead collectors ..	—	—	72
Miscellaneous ..	9·40	18·0	—
Electrical delays ..	83·05	139·53	350
Mechanical delays ..	31·1	410·2	Not known
Total tonnage handled	710 445	783 136	759 365
Electrical delay per 1 000 tons	3·98	1·07	2·32
Mechanical delay per 1 000 tons	1·5	3·15	Not known

(5.2.5) Application of Electric Magnets to Cargo Cranes Handling Ferrous Scrap.

Unless the level-luffing balanced-jib single-rope crane is specially arranged during the design stage, ring discharge grabs cannot be used, thus precluding the efficient grabbing of scrap iron. While not comparable in output to a grabbing crane, the fitting of electromagnets to general-cargo cranes can provide useful 'spillover' berths for handling such cargoes. The electro-magnets supplied to cargo cranes follow industrial practice, but some adaptations are required to meet dockside conditions. The range of lift may be 70 ft or more, making it difficult to use a spring-loaded cable drum for automatic winding of the flexible cable feeding the magnet. A sliding balance weight,

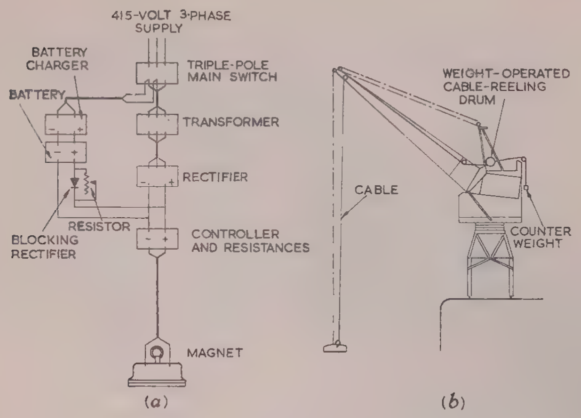

Fig. 13.—Scrap-handling magnets.
(*a*) Electrical schematic.
(*b*) General arrangement.

with a reversed pulley-block system, has proved satisfactory (Fig. 13).

Damage to the flexible magnet cable, allowing one magnet to drop the load, is an acceptable risk, but a supply failure causing five or more cranes working together on a vessel to drop their loads simultaneously cannot be countenanced. The solution is to feed each magnet from a battery floating across the supply, suitably trickle-charged by a metal rectifier, normal magnet operating current being supplied direct by a rectifier of greater capacity. Battery size should be sufficient to sustain the magnet load for about 15 min, giving time for the magnets to be lowered to the ground or the supply to be restored.

(5.3) Mobile Cargo Handling Plant

Mobile equipment for cargo handling must be robust enough to withstand abusive driving or overloading, and must be able to work over quayside surfaces interlaced with rail tracks, and upon dunnage in the holds. Specially designed equipment has been suggested, but the extra capital cost is difficult to justify, and standard industrial designs are accepted.

(5.3.1) Stillage Platform Trucks.

Battery-operated fixed and stillage-type platform trucks with 4-wheel steering are in general use. Troubles experienced are chiefly of a mechanical nature, e.g. worn track rods, tyres, damage due to collisions, etc. Apart from normal maintenance of controller contacts, motor brush-gear replacements and occasional commutator pitting by brush jump when passing too quickly over railway tracks, electrical troubles have been few. Batteries must be boxed and wedged to withstand vibration, and arranged for easy replacement. The guaranteed life of lead-acid batteries has been increased to four years by improved manufacturing techniques, but regular inspection and maintenance enables lives of 6–8 years to be attained. For 2-ton trucks, battery capacities of 160–350 Ah at 5-hour discharge rates and 28 and 40 volts are usual. While standard truck designs may not be possible, standardization of motor dimensions and voltages should be advantageous.

(5.3.2) Fork-Lift Trucks.

Battery-operated fork-lift trucks of 4 500–10 000 lb capacities are essential for modern cargo-handling practice, and in general are robust machines. Early designs were without interlocks for preventing movement when the driver's seat was unoccupied and for controller movement from full ahead to full astern, the latter giving the mechanical troubles of broken half-shafts. Battery capacities were also too small, but such faults have now been corrected by most makers. Maintenance accessibility is a difficult problem. Hinged contactor panels and motor mountings and similar improvements are assisting, but there remain such examples as the truck which requires the whole of its front to be stripped for access to the motor brush-gear, and the contactor panel so arranged that a loose connection necessitates complete removal of the panel. Such features must be eliminated.

Battery capacities range from 250 Ah for 4 500 lb trucks to 960 Ah for 10 000 lb trucks, with voltages of 36, 48 and 64 volts; the final remarks of Section 5.3.1 apply.

(5.3.3) Battery-Charging Equipment.

With increasing use of mobile plant, the individual charging unit, giving automatically the charge required by the battery's condition, is superseding the method of charging banks of batteries by motor-generator set. This is correct development, but decay of the selenium-rectifier capacity has been experienced.

(5.3.4) Mobile Cranes and Sack Pilers.

Diesel-electric mobile cranes are in general use, and are reliable machines with few electrical troubles. Early types had 15 min-rated travelling motors, and these were troublesome until replaced by 30 min-rated machines. Even to-day, motor design on some cranes shows weakness.

The sack-piler conveyor calls for little comment electrically. Since it is a portable appliance, with trailing cables subject to damage, its earthing arrangements should be carefully treated, preferably by fitting an earth-proving device.

(6) LIGHTING REQUIREMENTS

Dock-lighting requirements are embodied in the Docks Regulations, i.e. 'They shall be adequate to prevent danger'. The Factory Acts demand a minimum of 6 ft-candles for working areas, and 0·5 ft-candle for walking ways, and until values are defined in the Dock Regulations, the requirements of the Factory Acts are a useful guide. One thing is certain—the lighting conditions considered suitable a few years ago are not acceptable to-day. Tungsten-filament lamps were used for many years, mercury- and sodium-vapour units being considered sources of interference with signal lights, but this prejudice is being overcome.

(6.1) Fairway Buoys, Entrance Pier, Lead-in and Signal Lights

While negotiating the dredged channel, which is indicated by buoys, ships are guided by dock signal, 'lead in' and pier lights. Each harbour has its own pattern of coloured lights, with a fixed or flashing characteristic and a visibility range of 4–8 miles in clear weather. If situated in inaccessible places, they should be provided with duplicate lights or lamp-changing apparatus, failure of the lamp in circuit automatically bringing a sound lamp into focus.

Control is often effected by a selenium cell operating on the failing light, the flashing signal where required being effected by a timer with suitable cam switching the light on and off, or alternatively by inserting resistance to dim the light.

The increasing intensity of town lighting demands that the designers of the lamp optical systems are fully informed of such background lighting, otherwise disappointment in signal-light performance is inevitable.

(6.2) Lock-Entrance Lighting

Once the ship has negotiated the approach channel with the aid of lead-in lights, it must be brought into the lock in all

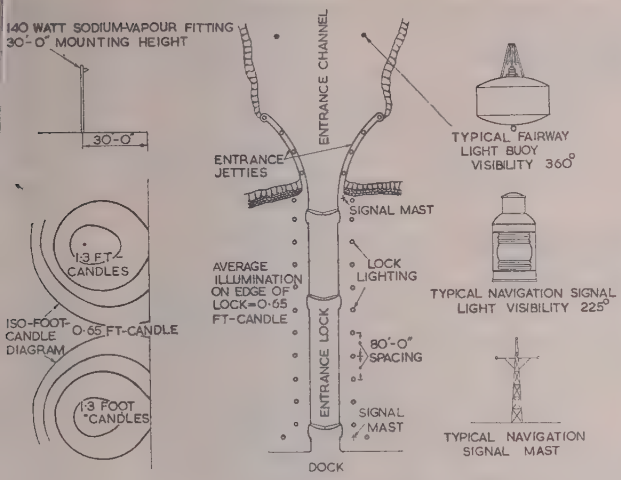

Fig. 14.—Dock-entrance lighting.

types of weather. Tungsten-filament lighting, often of low intensities, has been the general solution, but sodium-vapour lamps in floodlighting fittings are becoming increasingly accepted; the lack of glare, clarity of silhouette and clear definition of the lock edge are favourably commented upon, illumination values varying between 0·5 and 1·0 ft-candle being suitable. Fig. 14 gives the data relating to one of several installations which are giving excellent service.

Where aesthetic reasons and not capital costs prevail, street-lighting-type standards with underground cable supply are used, but often an acceptable minimum-cost solution is the wood-pole-and-tier type of overhead-line construction.

(6.3) Transit-Shed Lighting

In transit sheds used for general-cargo reception, label inspection for sorting to marks and intensive use of mobile mechanical-handling plant is necessary. Tungsten-filament lighting giving an average illumination of 1–3 ft-candles exists in many sheds, but most modern working requirements need the average 6 ft-candles of a factory installation.

The capital cost of mercury-vapour lamps is justified where the annual operating time exceeds 1 000 hours, but for many years colour distortion was thought to preclude their use. This has been exaggerated, and several installations are operating without complaint; but mixed tungsten-filament and mercury-vapour lighting, or the recently developed fluorescent-coated mercury-vapour lamps, would seem the best solution.

Ceiling-mounted fluorescent fittings can be used advantageously in sheds with exceptionally low roofs. Where lighting fittings can be supported out of reach of the mobile cranes, a satisfactory economic installation can be made by straining hard-drawn p.v.c.-insulated wires throughout the length of the shed, supported by porcelain insulators on the roof trusses, but the solid-drawn-conduit system seems more generally acceptable.

(6.4) Road Lighting

Dock estates have many miles of private roads illuminated by fittings mounted at 15–18 ft, spaced at 120–150 ft, with an illuminating value of average class-B standard, only the occasional major road necessitating class-A treatment. Directional fittings are in general use, although giving glare, but the advantages of sodium-vapour street-lighting fittings are recognized and these are being increasingly applied.

Tier-type overhead-line construction on wood poles is satisfactory for uncongested areas, street-lighting standards with cables being used for difficult sites or aesthetic reasons. If the fittings can be supported on buildings, solid-drawn conduit is used.

(6.5) Quayside and Reception Siding Lighting

The illumination of quaysides by broad-beam floodlighting fittings supported on the transit-shed eaves, mounted about 25 ft above the ground, spaced approximately 100 ft apart and giving an average illumination of 0·5 ft-candle, is satisfactory; the brightness can be increased to 2 ft-candles or more by switching on crane-pedestal floodlights during cargo operations. Tungsten-filament lighting is normal for such installations, since the damage rate is high, requiring minimum capital costs. Floodlighting towers carrying six floodlights or more, comprising narrow, medium and broad fittings suitably focused, each of 1–1·5 kW rating at 60–70 ft mounting heights and approximately 300 ft centres, are a noticeable trend for open berths, such towers being fed by underground cables and giving an even illumination of about 0·5 ft-candle over wide areas.

Different principles are applied to the lighting of reception sidings. It is unnecessary and uneconomic to illuminate such areas completely, since the shunters carry hand-lamps for signalling purposes after dark and their walking way is adequately lit. The aim should be to provide suitable lighting where tracks converge to railway points and crossings. Distributive fittings mounted on wood poles about 20 ft above the ground and fed by overhead-tier construction are adequate.

Where reception sidings converge to operating sidings of such equipment as coal hoists, general illumination of 1–2 ft-candles is provided in the working area. Broad-beam floodlighting fittings, supplemented where necessary by suitably sited distributive fittings, give satisfaction.

(6.6) Electrical Supplies for Shipping

In order to obtain a quick turn-round, many ships resort to double-shift working, often necessitating rigging of temporary lighting in the holds by means of t.r.s. cable and multiway couplers. For safety in these onerous working conditions, the voltage of supply should be 110 volts (55 volts to earth).

Both a.c. and d.c. supplies are required for shipping undergoing repair in the wet dock; a.c. supplies are taken direct from the crane plug-boxes or adjacent substations, and d.c. supplies are provided by portable motor-generator or rectifier sets.

(7) MAINTENANCE ORGANIZATION

While it is appreciated that wide variations of conditions and organization exist, when docks are grouped into large units the maintenance staff should be just sufficient at each dock to carry out efficiently the day-to-day maintenance, emergency repairs and small new works installations, with local responsibility for maintaining the unit to a set standard. To deal with peak demands of heavy maintenance and new works, h.v. cable and substation installations, etc., a mobile team, centrally based and directed, should be sent on a programmed basis to each dock as required. To comply with Docks Regulations and check that the set maintenance standard is achieved, a centrally controlled inspection team should examine the equipment annually, reporting their findings to the group electrical engineer and to the engineer of the dock concerned. This team should inspect all installations carried out by direct labour or by contractors on tenants' premises, to ensure compliance with The Institution's Regulations, and also should assist the professional engineering staff in the acceptance testing of large new works. A centrally sited winding repair and meter-test centre can be justified for such a group of ports.

While organizations vary widely, maintenance control is usually by allocation of specified staff to a dock area, giving them responsibility for maintaining the appliances and attending to any breakdowns occurring in that area. This method gives a sense of responsibility and effectively maintains the plant if the staff is of good quality, but is wasteful in manpower.

Recent work-study investigations confirm that maintenance men employed in this manner on day-work rates effectively work 40–50% of their booked time, but during breakdowns work harder and longer than operatives working at incentive speeds. Skilled-staff shortages and present wage rates cannot permit these wasted man-hours.

The alternative is fully planned preventive maintenance, with staff working at incentive speeds after the operations have been work-studied and rated, giving a higher wage rate to the staff and a lower maintenance expenditure with a considerable reduction in breakdown delays.

Fig. 15 shows the organization developed to suit dockside conditions, applied successfully at Newport Docks and in course of being extended throughout the South Wales Group; and Table 6 indicates also typical 'performance', 'delay' and 'not on bonus' time, showing effectively that, in addition to the greater effort of incentive conditions, considerable savings are possible by planning to eliminate waste time and improve methods. It is based on an inspection team, work-loaded to a specified schedule, examining the appliances on a tonnage-handled/time basis, their findings being reported to the supervisor, who allocates priorities before handing to the planning department, which work-loads the operative staff. It operates on a craftsman-and-mate basis, with increase in numbers to suit the specified job, each team being loaded daily with more than a full day's work. Delays must be reported immediately to the planning department for 'stop time' to be allowed. Minor breakdowns are met by allocating some staff to low-priority work with the overriding instruction to attend any emergency, reporting facts and duration to the planning office, a check on this being given by operating department records. Major breakdowns are reported to the planning officers, who redeploy the required staff.

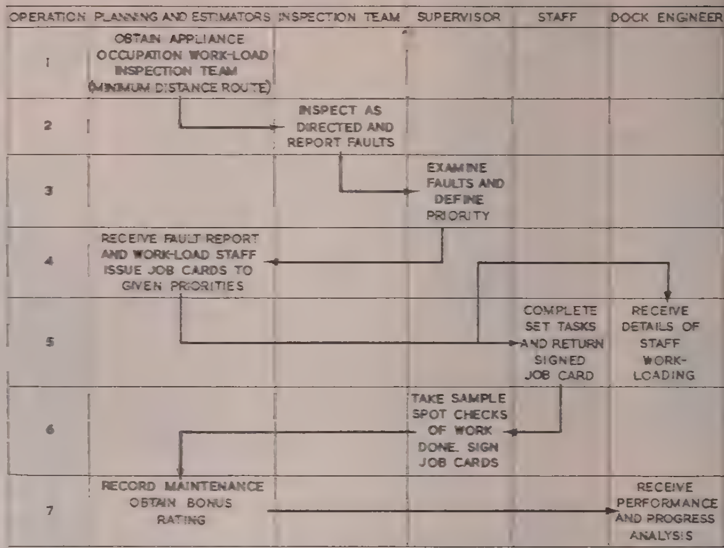

Fig. 15.—Planned-maintenance organization.

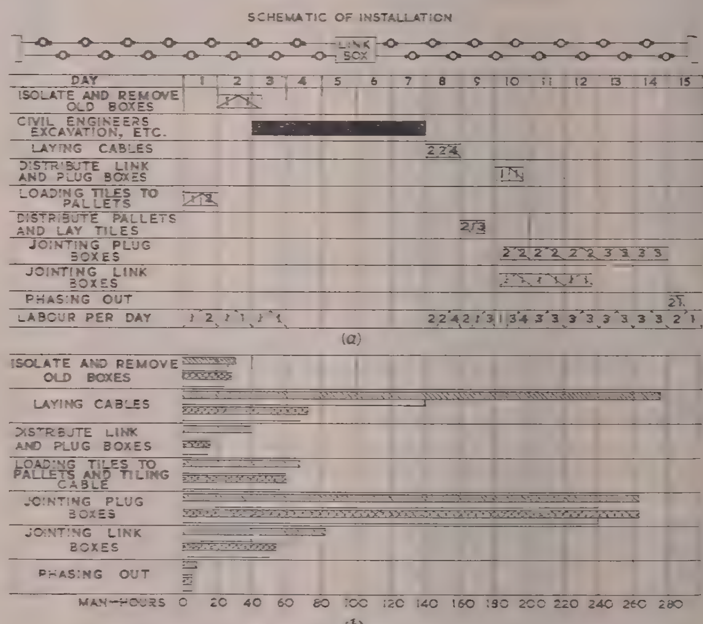

Fig. 16.—Method study for crane-service plug-box installation.

(a) Allocation of labour.
Plug box.
Joint.
Electricians.
Jointers.
Mates.

(b) Timings before and after method study.
Time allowed, Time taken } before method study.
Time allowed, Time taken } after method study.

Fig. 16 indicates the installation of a crane plug-box system carefully planned on the past experience of craftsmen, supervisors and engineers. While this was being installed the work was timed, rated and method studied, and the improved method, which has been employed many times since, was developed.

Table 6

PLANNED MAINTENANCE PERFORMANCE

Electrical section	Hours			
Total time on bonus	984	695	1 054	923
Bonus time paid	258	200	281	272
Operator performance	125	130	125	135
Total hours worked by section	1 160	956	1 194	1 243
Section performance	120	115	120	120
Delay times for week	3	—	6	4
Not on bonus time	173	261	133	316
Travelling time	—	—	—	—
No work owing to darkness	—	—	2	3
Working meal breaks	1	—	1	1
Delay time, %	0·26	—	0·5	0·36
Not on bonus time, %	14·8	27·3	11·2	25·5

(8) CONCLUSION

There may be many members of The Institution who from their personal knowledge and experience may not agree with all the subject-matter of the paper and may have more expert knowledge of some of the applications; but if the paper has stimulated thought and discussion in such a manner that the efficiency of electrical supply, distribution and utilization in the dock industry is improved, then it has succeeded in its object.

(9) ACKNOWLEDGMENTS

The author wishes to thank the General Manager and Members of the Docks Board of Management of the British Transport Commission for permission to prepare the paper, and also his friends and colleagues of the dock industry who have assisted by helpful criticism and a readiness to prepare diagrams and collect data for its presentation.

DISCUSSION BEFORE THE UTILIZATION SECTION, 9TH FEBRUARY, 1961

Mr. J. Monckton: Few people realize the extent of the engineering works in a dock installation; on the electrical side, especially, the range and scope are tremendous. Most of the installations in a dock system are governed entirely by local conditions, and one must take into consideration the sizes of the ships using the port, the type of traffic and many other variables.

However, there are points on which I disagree with the author. Low-voltage overhead distribution might be an economic solution to certain problems, but I would hardly call it satisfactory; in a dock system almost anything which can be put up can also be knocked down. With mobile cranes, which are constantly increasing in height, using the docks so much, overhead lines must be very carefully hidden.

I cannot support the author's preference for air-insulated busbar h.v. switchgear; we have had several unfortunate experiences at Liverpool, and I prefer compound-insulated busbars. For cables we find a p.v.c. sheath over the armour very satisfactory in operation and it prevents the corrosion of the armouring wires which otherwise occurs so rapidly in salt water. Of course, complete external insulation removes the advantages of earthing given by the armouring.

We can never get enough lighting at reasonable cost to satisfy users. At Liverpool virtually all of our transit-shed lighting consists of fluorescent twin-tube fittings, and these give satisfactory operation with economy.

I was surprised that the author dismissed rather summarily the use of Buchholtz protection on oil-filled transformers. It is difficult to protect an oil-filled transformer satisfactorily, because a fault is usually serious and may lead to a great deal of trouble—perhaps explosions. But there is a chance that the Buchholtz relay will operate before disaster occurs.

Mr. G. A. Wilson: Improvement in the efficiency of supply, distribution and utilization of electricity must be the object of many Institution papers, and certainly it is tremendously important in the dock industry. We have been asked to increase exports, and the transport of goods is one of the major links in the chain, so that anything which can be done to promote efficiency is worth while.

Electrical engineering is comparatively new in the dock industry, but as electricity replaces steam, hydraulic power, etc., the new equipment will require expert attention. It is no longer possible to get by with mere general knowledge of the subject. I am quite certain that, if industry will pay for the experts, costs can be cut and output increased.

The paper discusses problems similar to those which have been faced in London, and I am certain that a number of my electrical engineers will question some of the author's solutions. Could he discuss the quality of the electrical control gear used? Our working conditions are usually arduous and our atmospheric surroundings unpropitious, but great reliability is demanded; equipment to British Standards is of about the minimum quality permissible. Totally enclosed, or in some cases even steelworks grade, switchgear and control gear may be necessary.

It has been shown that wide electrical knowledge is called for in dock work; it may not be knowledge in depth, but in breadth it is considerable. The author has not covered the field completely, for radar devices, radiotelephones and electronic control gear are being used more and more.

Mr. H. Mounsten-Harrison: Regarding Section 3 of the paper, I would suggest that high-pressure hydraulic pumps should be sited as near the centre of gravity of the load as possible, to minimize pipe runs, rather than in the impounding station. There is a tendency to use self-contained units for individual application, thus obviating the heavy capital expenditure of central stations.

In Section 5.1 the author rejects d.c. systems, but for modern high-speed quay cranes a d.c. hoist motor is essential. Alternative a.c. drives are being used, but the preferred method with a.c. supplies is for a Ward Leonard drive on the hoist motion. Item 7 in Table 3 would be better defined as an 'opposed-torque' scheme rather than 'counter-current'.

The plug box in Fig. 10 shows the busbars at the bottom of the chamber, where condensation may cause trouble; also the plug projects above quay level. My preference is for watertight flush-fitting plug boxes having an automatic switch mechanism which operates when the plug is inserted.

Regarding the improved braking mentioned in Section 5.2.1, will the author state his requirements for maximum hook travel under the 'full-speed lower' to 'off' positions. What inertia has he in mind for a typical 6/3-ton crane-hoist mechanism (Section 5.2.3)?

Some form of standardization in battery-operated trucks is desirable, but this is difficult owing to the many factors involved. The battery sizes given appear very small, my experience being that a 28 V, 350 Ah battery is necessary for 2 ton platform trucks, and a 40 V, 1140 Ah battery for a 6000 lb fork-lift truck.

I agree generally with the author's comments on lighting, but would add that in transit sheds we have achieved excellent results with fluorescent lighting mounted 30 ft above floor level. What arrangements does the author make for security lighting during silent periods?

Mr. A. J. Parsons: Crane control has developed further since the paper was prepared, and I would draw attention to one scheme where the hoisting motions are controlled by a Ward Leonard system to which automatic features have been added. The scheme would appear to have merit where quick turn-round is a primary consideration. A special 3-field exciter with its output characteristics governed by the hoist-motor armature current is added to the conventional Ward Leonard set. By this means the fields of both the motor and generator are controlled simultaneously and automatically, so that the speed of the hoist motor is adjusted to suit the load on the hook at the time.

Fig. A shows the approximate characteristics of the scheme, which should be compared with the author's Fig. 12(*f*). The top-notch characteristics are stepless, a feature which eliminates snatching in the crane operation. On the hoisting side, a speed range of 4 : 1 can be obtained (from 400 ft/min at light hook to 100 ft/min on full load). The lowering characteristics are also interesting, in that there is a fall in speed when load is applied.

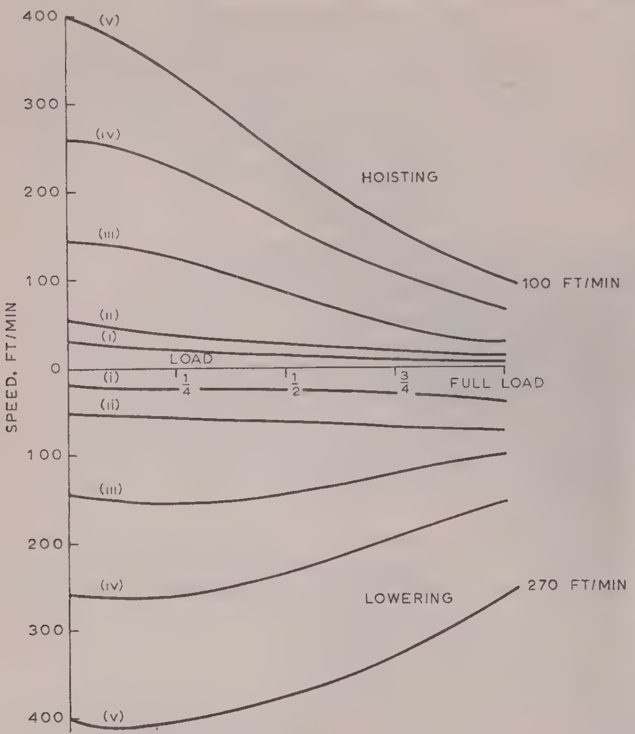

Fig. A.—Approximate performance characteristics of hoist system.

(i) Notch 1 (master controller).
(ii) Notch 2.
(iii) Notch 3.
(iv) Notch 4.
(v) Notch 5.

This inherent control over lowering limits the kinetic energy built up in the hoist motor and load, and by choosing a full-load figure of about 66% of the light-hook speed, the loaded stresses in the crane and the electrical machines are no more than the light-hook stresses.

I endorse the author's remarks on the inertia of hoist-motor armatures. With conventional geared units they represent a major proportion of the total inertia and the best performance is obtained from long, small-diameter motors.

The author implies that some improvement might result from taking full advantage of the temperature rises permitted by British Standards, but it should be remembered that the size of the motor is quite often governed by performance and not by temperature rise. In d.c. machines increased temperature rise also increases the drift in speed from cold to hot, especially with series-wound motors. What tolerance would the author be prepared to accept from this effect?

Table 3 does not relate costs to performance, although one might deduce from it that the higher the cost the better the performance. From his available data, could the author compute a figure of merit for each of the types mentioned? Such a figure would be useful in making a selection of crane machinery.

Mr. M. Rundle: D.C. quay cranes have proved very reliable when working at high speeds with a wide range of loads, and although direct current is uneconomical for distribution, the series motor can still be retained if unit rectifiers are used; semiconductor devices may provide a satisfactory solution. Most of the control systems described in the paper are voltage sensitive. Has the author had difficulty in controlling the power factor? Has voltage fluctuation given rise to working difficulties and have larger cable cross-sections been required?

A projecting plug box as shown in Fig. 10 would be unacceptable on a mechanized quay as it would be a possible cause of personal injury and cargo damage. We have successfully tried a scheme where the plug box is mounted in a recess in the dwarf brick wall of the transit shed. The trailing cable lies in a groove formed in the quay by double bulb angle strips. The brickwork is extended into the shed to form a shallow disconnection cubicle. In sheeted buildings a steel box is recessed into the side of the shed to carry the plug box. Surely the incidence of breakdowns due to trouble with a plug box and trailing cable is related to the volume and type of quay traffic?

In the maintenance organization described, does the inspection team undertake maintenance work, and how far in advance does the author arrange with the user departments to take over an appliance? Since the arrival date of ships is uncertain, forward planning in docks is difficult. Thus men are retained on an hourly-paid basis, since they are then able to carry out routine maintenance at short notice without interrupting dock working—a system which may be less costly than adopting other means of cargo handling. In the scheme described, was the overall number of staff reduced or were skilled maintenance men replaced by clerical workers?

Mr. R. Quigley: At the Port of London the quayside cranes must handle a variety of goods, and at some berths this includes grabbing duties as well as general cargo. It has been found that a crane on grabbing duties makes the biggest demand on the power supplies, but it is unlikely to exceed 40–45 kVA for a 5-ton 80 ft crane and 35–40 kVA for a 3-ton 80 ft crane of modern design. Where general cargo is handled the demand may be approximately 75% and may well be as low as 50% of the grabbing duties. A diversity factor for, say, five cranes on similar duties would be about 0·75. Some typical consumption figures for cranes handling general cargo were found to be between 0·5 and 1 kWh per ton, which agree with those given in Fig. 7.

In comparison with Fig. 2, the numbers of substations per 1 000 yd for four typical dock-distribution systems in London are 1·4, 1·4, 1·6, 1·6, and the ratio between transformer power (including rectifiers) and system maximum demand varies between 1·8 and 2·5. The higher figures for this ratio, compared with those for the South Wales Docks, are possibly due to the use of d.c., as well as a.c., distribution systems within the docks.

We fit Buchholz relays to transformers of 500 kVA and above in congested dock areas where there is a risk of fire to adjacent buildings or warehouses. Where we are unable to give satisfactory overload protection to transformers with current-transformer-operated relays, because of the shortage of time to discriminate with other sections of the system, we fit Buchholz relays and oil temperature alarms. In modern unmanned substations the operation under fault of Buchholz relays, oil-temperature alarms, carbon dioxide extinguishers, and relays on the h.v. and m.v. oil circuit-breakers is arranged to energize a master fault-relay within the substation, which in turn operates an alarm at a central point.

I should like to know what type and construction of cable cleat is used under water at South Wales Docks.

Mr. D. S. Durston (*communicated*): I cannot accept the suggestion that a 0·06 in² cable can be used on a 10-ton grabbing crane (Section 5.1). Fig. 7 shows that such a crane has a maximum demand of 250 A at 415 V and agrees with figures I have obtained. A 0·06 in² flexible cable is rated at 71 A so that either The Institution's Wiring Regulations are unrealistic or the author is operating these cables very near their limit of safety.

A 120 ft cable of 0·06 in² section has a line-voltage drop of

30 V at 800 A, which is a typical peak accelerating current. At this instant the highest voltage possible is required to increase the motor torque. This is surely taking size reduction too far.

I would add 'minimum interior air space' to the author's plug-box specification (Section 5.1.1), to reduce condensation; also, the conductors should be well clear of the bottom of the box.

With self-reeling cable drums passing over the top of the plug boxes, I have found that rocking causes burned-out pins because intimate contact is lost. This problem has been completely overcome by making the plugs parallel-sided and locating the tops in registers, thus preventing rocking.

I have also encountered crane motors having a very low temperature rise when working continuously on the maximum duty cycle specified to the crane manufacturer. However, I would commend the pull-out torque as a more fruitful subject for investigation; while low temperature rise is reflected in first cost only, poor torque characteristic affects the whole electrical and mechanical chain.

Mr. K. K. Schwarz (*communicated*): I should like to raise two points regarding the integrated design of rotating machines and their control systems, involving the economic evaluation of the efficacy of a drive.

The hydraulic problems posed in Section 3 are, in the industrial field, frequently met and solved by the application of variable-speed a.c.-commutator-motor drives for single and multiple working. The wide experience available might well solve these apparently similar problems, and, as usual, the increased first cost must be offset against performance and efficiency, compared with the use of the 'lossful' regulation systems mentioned. The proved reliability of a.c. variable-speed motors with induction-regulator control eliminates any doubts on this account.

The tabulation of crane control systems (Table 3) reveals the economics quite clearly when relating drive suitability to first cost. Furthermore, the questions of rating, forced ventilation and low-motor-inertia designs are all involved in these economic and performance considerations.

The 'opposed-torque' cranes, for instance, have intermittancy ratings in excess of 50%, necessitating the forced ventilation mentioned, and are also designed with low motor inertia, thus achieving accelerating times to base speed of the order of 1–$1\frac{1}{2}$ sec, and plugging times of approximately 2 sec for full load, double the speeds being available for half load with proportionately increased accelerating and plugging times.

Generally, a high-performance crane necessitates a higher rating, owing merely to its inherent efficacy and consequent higher potential usage. Similarly, power consumption (Fig. 7) should be related to the total work done, i.e. the utility of the equipment as a whole in moving a given tonnage in a given time, and further information on this point would be most interesting.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSION

Mr. E. R. Radway (*in reply*): It is pleasing to note the appreciation of the wide range and types of plant encountered, and the recognition that geographical siting, dock layout and types of cargo handled affect both the plant and the attitude of the responsible engineer.

I am grateful to Mr. Wilson for mentioning the light-current and communication fields, which are increasing in importance.

Several speakers favour Buchholz protection of transformers, and Mr. Monkton prefers compound-filled h.v. switchgear, but transformers and air-insulated busbar switchgear are so reliable that the extra cost of these items is difficult to justify. Details of specific cases to support the views of these speakers would have been welcomed. The information given by Mr. Quigley regarding the number of substations per 1000 yd and ratio of transformer capacity to system demand at the London docks seems to agree fairly well with the South Wales practice. Cable cleats which are used under water comprise suitably reinforced teak blocks.

It is agreed that low-voltage overhead distribution is not practicable in congested dock areas, but many situations exist, e.g. coal-hoist approach roads and railway sidings, where such distribution is both economic and satisfactory.

Lighting is always a problem, and higher standards are continually demanded. Several speakers mentioned the use of fluorescent fittings in transit sheds, at mounting heights of 30 ft or more, but I consider a concentrated-light-source fitting is better for such applications. Special security lighting does not seem justified if the circuits are properly sectionalized.

It is correct that high-pressure hydraulic pumps should be sited near the centre of gravity of the load (this is often convenient for the impounding stations) and that hydraulic load is reducing.

Hydraulic turbine pumps need constant-speed drives, and the variable-speed a.c. commutator motor mentioned by Mr. Schwarz is unsuitable, although it might be used for the ram-type pumps. During the load-shedding period of 1946–52, supply-frequency reduction caused hydraulic turbine pumps to cease pumping.

The general measure of agreement that large-radius high-speed cranes need the d.c. motor characteristic for the hoist motion, and that motor inertia must be kept to a minimum, is noted.

At this stage of development the Ward Leonard set—especially the modified one described by Mr. Parsons—gives the best characteristic for minimum operating maintenance and reasonable capital cost for cargo cranes, but for 4-line 2-motor grabbing cranes, counter-current a.c. schemes give satisfaction. As Mr. Rundle comments, the rapid developments in semiconductor rectifiers may soon provide an alternative solution. Power-factor control and voltage fluctuation have not caused difficulties. The drift in speed from cold to hot mentioned by Mr. Parsons should not be serious in practical crane operation, and I should not worry with a tolerance of 5–10%.

With electrical braking the distance covered while decelerating from full speed at full load to standstill need not exceed 4 ft. Hoist-motor inertia for the low-speed 3-ton cranes is 13 500 ft-lb, approximately, increasing to as much as 85 000 ft-lb for high-speed 10-ton twin-barrel grabbing cranes. Probably the most arduous duty at a quay is that applicable to grabbing cranes unloading iron ore, and the British Standards adequately meet such requirements. Mr. Quigley's information on maximum demand and consumption is interesting, for it seems to give details of grabbing duties applied to single-line cargo cranes, presumably with self-dumping grabs, rather than to 4-line high-capacity grabbing cranes.

I do not favour an automatic switch mechanism which operates when the plug is inserted into the crane plug-box. The plug should be inserted dead, and the switch operation should mechanically interlock the plug. Adequate clearance exists between the busbars and the bottom of the chamber, and no serious condensation trouble has been experienced; neither, in my experience, has personal injury or damage been caused by this type of plug box on a mechanized quay.

Mr. Durston's disagreement on trailing-cable sizes is understandable, and his general comments most interesting. Emphasis should be placed on having a good electrical supply system,

with plug boxes at 50ft centres, keeping trailing-cable lengths to a minimum. Trailing cables are subject to damage, and first cost should be kept low with the cables operated at their maximum safe current rating. The Institution's Regulations to which Mr. Durston refers are for continuous rating, and large intermittency factors should be applied. With cable lengths of 60–70ft the voltage drops are reasonable, and no difficulty from low starting torques has been experienced.

In reply to Mr. Rundle, the inspection team does not undertake maintenance work. Its duty is to inspect thoroughly and find all that requires attention, so enabling work to be planned on a priority basis and to be organized to integrate with the requirements of the using department. With the planning organization available it is sufficient if the using department gives clearance by 2p.m. on the day prior to occupation.

In the scheme described, the emphasis was placed on minimum clerical work and the number of staff was reduced considerably, so that, even after extra incentive payments, a considerable overall financial saving was made, with better maintenance and fewer operational failures.

DISCUSSION ON 'ENGINEERING EDUCATION AT THE TECHNICAL UNIVERSITIES IN WESTERN GERMANY'*

Dr. W. A. Gambling (*communicated*): Recently I had the good fortune to visit the Technical Universities at Aachen, Karlsruhe, and Munich under the auspices of the British Council, and also the Chalmers University of Technology in Sweden. In addition, I have been closely associated for several years with a German technical graduate. I therefore read the above paper with a great deal of interest, and found it an admirable survey of the German technical universities, which are so very different from the universities in this country. It should be pointed out that my interests lie in the field of electronics, and I visited only the appropriate high-frequency institutes of the above universities.

One criticism which is often made of German technical universities is that too much emphasis is laid on techniques to the detriment of fundamentals. As far as the high-frequency institutes are concerned this certainly does not appear to be the case, as the fundamental theory is covered very thoroughly. Indeed there is a certain amount of duplication, since the same ground is often covered in more than one institute. A more justifiable criticism might be that perhaps there is not enough co-ordination between the courses given in the various institutes. More time is devoted to practical techniques than is possible in this country, as the authors state, because this is part of the function of the technical universities and the course is much longer. An interesting feature is that the professor often makes his notes available to the students in book form. Since these need revision from time to time a considerable amount of work can be involved. One advantage of the lack of specialization in the schools is that many of the recommended course books are English texts. One can imagine the reaction of the average English engineering undergraduate on being required to consult French or German textbooks.

Since most, if not all, of the lecturing is done by the head of the institute, his teaching load is often staggering by English standards, and in addition he is responsible for the administration of the institute and is expected to help in the running of the university, in the usual way, by serving on various committees. The amount of time which the professor can devote to research is thus very limited, and moreover there is no permanent lecturing staff to help in the supervision of research. This is perhaps one of the most serious failings of the German system. The calibre of the research which is done is thus a tribute to the industry and the fortitude of the professors. A great merit of the technical universities is the close co-ordination between academic and practical work and the fact that the latter must be satisfactorily completed before the final examination.

Although it is perhaps unwise to generalize, it is my impression that English engineering honours graduates on the whole have the advantage of an academic training comparable with that of their German counterparts, but that they would benefit greatly from a closer link between their practical training and formal studies.

* WELBOURN, D. B., SPALDING, D. B., and ASHDOWN, G. L.: *Proceedings I.E.E.*, Paper No. 2913, May, 1959(see **106 A**, p. 409).

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THE INDUCTANCE COEFFICIENTS OF A SALIENT-POLE ALTERNATOR IN RELATION TO THE TWO-AXIS THEORY

By Professor G. W. CARTER, M.A., Member, W. I. LEACH, M.Sc., Graduate, and J. SUDWORTH, M.Sc., Student.

(*The paper was first received* 19*th November*, 1960, *and in revised form* 27*th February*, 1961.)

SUMMARY

The simplicity of the machine equations in their two-axis form depends upon a certain relation between the inductance coefficients being satisfied. The condition is likely to be fulfilled in a machine having a uniform air-gap, but in a salient-pole machine its fulfilment is open to doubt. The paper contains a theoretical and experimental study of the question: the experimental aspect has required the development of new methods of measuring the machine constants.

LIST OF SYMBOLS

b = Semi-thickness of lamination.
B_0 = Flux density at surface of lamination.
F = Magnetomotive force on pole.
$\mathrm{h}(\alpha)$ = Number of phase conductors in a certain belt.
H_0 = Magnetizing force at surface of lamination.
I = Peak value of phase current.
I_a, I_x = R.M.S. values of i_a and i_x.
i_a, i_b, i_f = Currents in windings a, b and f (instantaneous values).
i_d, i_q = Equivalent currents on direct and quadrature axes.
i_x = Current equivalent to eddy currents in pole iron.
f_a, f_b = Current-turns in phases a and b in a certain linkage.
L_{aa}, L_{bb}, L_{ff} = Self-inductances of windings.
L_{ab}, L_{af}, L_{bf} = Mutual inductances between windings.
L_{ad} = Maximum of fundamental component of L_{af} and L_{bf}.
$L_c = \frac{1}{2}(L'_D - L_D)$.
L_D = Maximum of fundamental component of L_{aa} and L_{bb}.
L'_D = Maximum of fundamental component of L_{ab}.
L_d, L_q = Inductances of armature on direct and quadrature axes.
L_E = Inductance correction on account of eddy currents.
L_S = Mean value of L_{aa} or L_{bb}.
L_x = Inductance of winding representing eddy currents in pole iron.
L_{xd} = Mutual inductance between armature and above winding.
l = Length of pole.
N = Number of turns per phase.
N_x = Number of turns in winding representing eddy currents in pole iron.
n = Number of laminations in pole.
$\mathrm{n}(\alpha)$ = Function defining distribution of armature turns.
n_1 = Coefficient of fundamental component of $\mathrm{n}(\alpha)$.
$\mathrm{p}(\alpha + \theta)$ = Function defining variation of air-gap permeance.
p_0, p_2 = First two coefficients in $\mathrm{p}(\alpha + \theta)$.
q = Number of slots per pole per phase.
r_a = Resistance of armature phase.
r_e = Effective resistance of armature phase at standstill.
r_f = Resistance of field winding.
r_x = Resistance of winding representing eddy currents in pole iron.
s = Fractional slip.
V_a, V_b = R.M.S. values of v_a and v_b.
v_a, v_b, v_f = Terminal voltages on windings a, b and (instantaneous values).
P_e = Eddy-current loss.
w = Width of pole.
X_e = Effective reactance of armature phase at standstill.
$X = L\omega$, where L is the corresponding inductance.
y_x = Multiplier having dimensions of admittance.
$Z_x = (r_x^2 + X_x^2)^{1/2}$.
α = Angle defining position on armature.
β = Dimensionless parameter associated with flux penetration.
η_0 = Primary magnetic constant ($4\pi \times 10^{-7}$).
θ = Angular position of armature.
μ = Permeability (relative).
ρ = Resistivity.
Φ = Flux.
Φ_a = Flux linkage associated with phase a.
Φ_0 = Total flux in lamination at low frequency.
Φ_p, Φ_q = Components of flux in phase and in quadrature with applied m.m.f.
ω = Angular frequency of supply.

(1) INTRODUCTION

The treatment of synchronous-machine problems by the two-reaction method, originated by Blondel[1, 2] and developed by Doherty and Nickle,[3] Park,[4] and others, has been generally accepted. It is not always realized, however, that the two-reaction theory takes for granted a certain relation between the inductances of the armature windings, which is satisfied in a conceivable idealized machine but which is not likely to hold good in salient-pole machines as they are actually constructed. In view of the large structure of generalized machine theory which has now been built upon the two-reaction or two-axis method, it appears desirable to investigate this fundamental assumption.

The point at issue is best explained by referring to the simple 2-phase machine shown in Fig. 1: the convenience of treating polyphase machines in terms of a 2-phase equivalent is already recognized. The field system, regarded as the stationary member, sets up a magnetomotive force in the sense indicated by the arrow i_f, which coincides with the direct axis of the machine; the rotor carries the 2-phase armature winding with m.m.f.s indicated by the arrows i_a and i_b, and rotates in the

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
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direction shown. If the machine is a motor, the potential differences required at the terminals of the three windings are related to the currents by the equations:

$$\left.\begin{aligned} v &= \frac{d}{dt}(L_{ff}i_{f} + L_{af}i_a + L_{bf}i_b) + r_f i_f \\ v_a &= \frac{d}{dt}(L_{af}i_f + L_{aa}i_a + L_{ab}i_b) + r_a i_a \\ v_b &= \frac{d}{dt}(L_{bf}i_f + L_{ab}i_a + L_{bb}i_b) + r_a i_b \end{aligned}\right\} \quad . \quad . \quad (1)$$

The inductances L_{ff}, L_{af}, etc., are, in general, functions of θ, so that they will vary as the armature rotates. If they are expressed as Fourier series in θ, considerations of symmetry enable us to discern what the forms of the series will be, and it is found that the initial terms can be written

$$\left.\begin{aligned} L_{ff} &= L_F \text{ (a constant)} \\ L_{aa} &= L_S + L_D \cos 2\theta \\ L_{bb} &= L_S - L_D \cos 2\theta \\ L_{af} &= L_{ad} \cos\theta \\ L_{bf} &= L_{ad} \sin\theta \\ L_{ab} &= L'_D \sin 2\theta \end{aligned}\right\} \quad . \quad . \quad . \quad . \quad (2)$$

These expressions are obtained, with minor changes, by Gibbs.[5] When they are substituted into eqns. (1), they lead to a set of equations which may be expressed in the matrix form*

$$\begin{bmatrix} v_f \\ v_a \\ v_b \end{bmatrix} = \begin{bmatrix} L_F D + r_f & L_{ad}D\cos\theta & L_{ad}D\sin\theta \\ L_{ad}D\cos\theta & D(L_S + L_D\cos 2\theta) + r_a & L'_D D\sin 2\theta \\ L_{ad}D\sin\theta & L'_D D\sin 2\theta & D(L_S - L_D\cos 2\theta) + r_a \end{bmatrix} \begin{bmatrix} i_f \\ i_a \\ i_b \end{bmatrix} \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

The change from phase currents and voltages to two-axis currents and voltages is performed by making the substitutions

$$\left.\begin{aligned} i_a &= i_d\cos\theta + i_q\sin\theta \\ i_b &= i_d\sin\theta - i_q\cos\theta \end{aligned}\right\} \quad . \quad . \quad . \quad . \quad (4)$$

with a similar relation for voltages. This transforms eqns. (3) into the following:

$$\begin{bmatrix} v_f \\ v_d \\ v_q \end{bmatrix} = \begin{bmatrix} L_F D + r_f & L_{ad}D & \\ L_{ad}D & (L_d + L_c)D + r_a - L_c(c_4D - 3s_4\dot\theta) & (L_q - L_c)\dot\theta - L_c(s_4D + 3c_4\dot\theta) \\ -L_{ad}\dot\theta & -(L_d + L_c)\dot\theta - L_c(s_4D + 3c_4\dot\theta) & (L_q - L_c)D + r_a + L_c(c_4D - 3s_4\dot\theta) \end{bmatrix} \begin{bmatrix} i_f \\ i_d \\ i_q \end{bmatrix} \quad . \quad . \quad (5)$$

where $L_d = L_S + L_D$, $L_q = L_S - L_D$, $L_c = \frac{1}{2}(L'_D - L_D)$, and c_4 and s_4 are abbreviations for $\cos 4\theta$ and $\sin 4\theta$. In the particular case where $L'_D = L_D$, so that $L_c = 0$, this takes the simple form

$$\begin{bmatrix} v_f \\ v_d \\ v_q \end{bmatrix} = \begin{bmatrix} L_F D + r_f & L_{ad}D & \\ L_{ad}D & L_dD + r_a & L_q\dot\theta \\ -L_{ad}\dot\theta & -L_d\dot\theta & L_qD + r_a \end{bmatrix} \begin{bmatrix} i_f \\ i_d \\ i_q \end{bmatrix} \quad (6)$$

It is apparent from the foregoing that the 2-axis transformation brings about a significant simplification of eqns. (3) only when the coefficients L_D and L'_D are equal. If they are not equal, eqns. (6) are modified by the substitution of $L_d + L_c$ and $L_q - L_c$ for L_d and L_q, and complicated by the addition of terms in $\cos 4\theta$ and $\sin 4\theta$ which make them quite unmanageable.

It is therefore important to determine whether the coefficients L_D and L'_D are equal or nearly equal in practice. (The question concerns salient-pole machines only; in machines with uniform air-gaps, both coefficients should be zero.) More broadly, it is desirable to ascertain whether the machine inductances are adequately described by eqns. (2), and whether the coefficients L_S, L_D, L'_D and L_{ad}, which are assumed to be constant, are so in fact. The investigation is carried out of necessity on a particular machine, but the results are interpreted in such a way as to throw light on the properties of salient-pole machines in general.

* The symbol D is used as an abbreviation for d/dt instead of the p employed by some writers, because the symbol p, as employed in the operational calculus, is not a synonym for d/dt.[6]

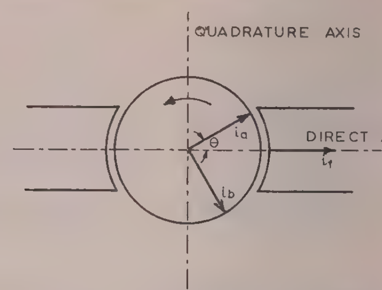

Fig. 1.—Elementary 2-phase machine.
The angle α is measured anti-clockwise from the axis i_a.

(2) IDEAL 2-PHASE MACHINE

We will examine, to begin with, how far the observed properties are accountable to the distribution of the winding in the machine tested and to the nature of its air-gap permeance.

To this end we consider the idealized machine, shown in Fig. 1 at the instant when phase a makes an angle θ with the direct axis. The iron is assumed to be infinitely permeable, and the whole flux passes round the main magnetic circuit, i.e. leakage flux is neglected. The number of conductors from phase a contained in the arc $\delta\alpha$ is taken as $n(\alpha)\delta\alpha$, $n(\alpha)$ being some function of α; the current being oppositely directed for negative values of α, $n(\alpha)$ will be an odd function. The phases being assumed identical, the number of conductors from phase b in the arc $\delta\alpha$ must be $n(\alpha + \frac{1}{2}\pi)\delta\alpha$. The permeance of an arc $\delta\alpha$ of the air-gap, being dependent on the angle $\alpha + \theta$ of the arc from the direct axis, may be written $p(\alpha + \theta)\delta\alpha$, where p is an even function.

The self-inductance of phase a is L_{aa}, given by

$$L_{aa}i_a^2 = \Sigma f_a \delta\Phi_a \quad . \quad . \quad . \quad . \quad . \quad . \quad (7)$$

where $\delta\Phi_a$ is an element of the flux set up by i_a, f_a is the total of the current-turns linked with this tube of flux, and the summation covers all the tubes. The flux $\delta\Phi_a$ is set up by the operation of the m.m.f. f_a upon two air-gaps of angular width $\delta\alpha$, and is therefore given by

$$\delta\Phi_a = \tfrac{1}{2}f_a p(\alpha + \theta)\delta\alpha\,.$$

Also

$$\begin{aligned} f_a &= i_a\int_{\alpha}^{\pi-\alpha} n(\alpha)d\alpha \\ &= i_a h(\alpha) \text{ (say)} \end{aligned}$$

Substituting into eqn. (7) we obtain

$$L_{aa} = \tfrac{1}{2}\int_{-\pi/2}^{\pi/2} [\mathrm{h}(\alpha)]^2 \mathrm{p}(\alpha + \theta) d\alpha \quad . \quad . \quad . \quad . \quad (8)$$

Similarly the mutual inductance L_{ab} is given by

$$L_{ab} i_a i_b = \Sigma f_b \delta\Phi_a \quad . \quad . \quad . \quad . \quad . \quad . \quad (9)$$

where f_b is the total current-turns in phase b linked with the tube of flux $\delta\Phi_a$ set up from phase a. By the same substitutions, we obtain

$$L_{ab} = \tfrac{1}{2}\int_{-\pi/2}^{\pi/2} \mathrm{h}(\alpha)\mathrm{h}(\alpha + \tfrac{1}{2}\pi)\mathrm{p}(\alpha + \theta) d\alpha \quad . \quad . \quad (10)$$

If $\mathrm{n}(\alpha)$ and $\mathrm{p}(\alpha + \theta)$ are expressed as Fourier series, the initial terms must be

$$\left.\begin{aligned} \mathrm{n}(\alpha) &= n_1 \sin\alpha + \ldots \\ \text{and} \quad \mathrm{p}(\alpha + \theta) &= p_0 + p_2 \cos(2\alpha + 2\theta) + \ldots \end{aligned}\right\} \quad . \quad (11)$$

If all other terms are neglected, substituting these into eqns. (8) and (10) leads to the results

$$L_{aa} = \pi n_1^2 p_0 + \frac{\pi n_1^2}{2} p_2 \cos 2\theta \quad . \quad . \quad . \quad (12)$$

and

$$L_{ab} = \frac{\pi n_1^2}{2} p_2 \sin 2\theta \quad . \quad . \quad . \quad . \quad . \quad . \quad (13)$$

These are exactly of the form set down in eqns. (2), namely $L_{aa} = L_S + L_D \cos 2\theta$, $L_{ab} = L_D' \sin 2\theta$; and in this case $L_D = L_D'$, so that the fundamental condition which forms the subject of this investigation is satisfied. But the assumptions used to prove it—sinusoidally laid windings and sinusoidally varying permeance—are idealized to the point of unreality. So far as the permeance is concerned, the inclusion of higher harmonics has the effect of inserting higher-harmonic terms into L_{aa} and L_{ab}, without affecting the terms given above; on the other hand, harmonics in the winding distribution would alter the coefficients in eqns. (12) and (13).

We therefore turn to the more practical conception of a machine in which the winding is concentrated in q slots per pole per phase, there being no overlap between phase windings. A detailed analysis for such a machine is carried out in Section 6.1, in which are obtained the following results. For the self-inductance of either phase,

$$L_{aa} = k_0 N^2 p_0 + k_2 N^2 p_2 \cos 2\theta + \ldots \quad . \quad . \quad (14)$$

where

$$k_0 = \frac{\pi}{6}\left(2 + \frac{1}{q^2}\right) \quad . \quad . \quad . \quad . \quad . \quad . \quad (15)$$

$$k_2 = \frac{\cos(\pi/2q)}{q^2 \sin^2(\pi/2q)} \quad . \quad . \quad . \quad . \quad . \quad . \quad (16)$$

For the mutual inductance between phases,

$$L_{ab} = k_2' N^2 p_2 \sin 2\theta + \ldots \quad . \quad . \quad . \quad (17)$$

where

$$k_2' = \frac{1}{q \sin(\pi/2q)} \quad . \quad . \quad . \quad . \quad . \quad . \quad (18)$$

Table 1 contains the numerical values of the multipliers given by eqns. (15), (16) and (18) for various numbers of slots per pole per phase, the values obtained with an ideal sinusoidal winding being also cited for comparison.

It will be seen that the condition $k_2 = k_2'$ is far from being satisfied for any winding in which the phase conductors form uniform belts over appropriate portions of the armature periphery.

Table 1

Calculated Inductance Constants for 2-Phase Windings

q	k_0	k_2	k_2'
1	1·571	0	1·000
2	1·178	0·354	0·707
3	1·105	0·385	0·667
4	1·080	0·394	0·653
5	1·068	0·398	0·647
6	1·062	0·401	0·644
..	..	..	..
∞	1·047	0·405	0·637
Sinusoidal winding	0·785	0·393	0·393

(3) EXPERIMENTAL INVESTIGATION

With a view to determining how far these ideal relationships hold good in an actual machine, measurements were made on a 2-phase 12-pole 50 c/s salient-pole alternator, rated at 10 kW, 220 V. Design data were not available, but it could be seen from inspection that each phase winding occupied 4 slots per pole (single layer). There were no damper windings.

(3.1) Measurements with Machine Stationary

Using the simple voltmeter-ammeter-wattmeter method with a 50 c/s supply on one phase, the self-inductance of that phase and the mutual inductance between the two phases were measured with the machine stationary. The induced voltage in the field winding was used to indicate the position of the principal axes of the machine. The results of these tests are shown in Fig. 2.

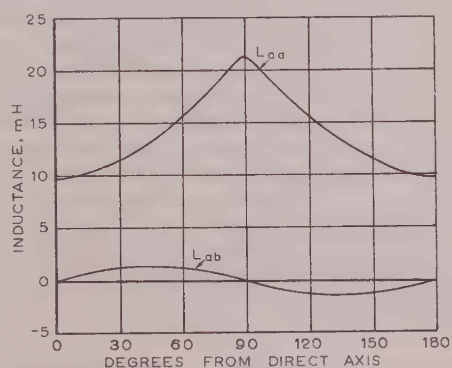

Fig. 2.—Inductance measurements at power frequency with the machine stationary.

L_{aa} Self-inductance of phase a.
L_{ab} Mutual inductance between phases a and b.

The curve of mutual inductance between phases, though small in amplitude, possesses the expected shape and position. The curve of self-inductance, on the other hand, differs from the ideal assumed in eqns. (2), not only by reason of its non-sinusoidal shape but also in having its maximum when the axis of the phase is in line with the quadrature axis, that is, with the axis on which the magnetic reluctance is greatest. Though at first sight unexpected, this result is easily explained by considering how the magnetic circuit is constructed. The pole system, being designed to carry only a constant flux, is built up from thick plates of iron. When a phase winding is aligned with the pole centres (i.e. located on the direct axis), the flux set up by current in the winding must almost entirely traverse the main magnetic circuit of the machine. With alternating current, the passage of flux round this path is largely inhibited by induced

currents in the pole iron; the reluctance of the magnetic path is high, and the apparent inductance of the phase winding low. When the phase winding is on the quadrature axis, on the other hand, the flux path is shorter, consisting of the armature iron, the air-gap and the pole shoes. Thus, although the area of iron presented to the winding is less than in the direct-axis position, the total flux linkage, and hence the inductance, may be greater.

The effect of eddy currents in the pole iron is discussed in Section 6.2, and the qualitative explanation given above is translated into quantitative terms in Section 6.3. It is shown that the apparent inductance and mutual inductance of the phases, as measured with the machine stationary, take the form

$$\left.\begin{array}{ll}\text{Self-inductance} & = (L_S - L_E) \pm (L_D - L_E)\cos 2\theta \\ \text{Mutual inductance} & = (L'_D - L_E)\sin 2\theta\end{array}\right\} \quad (19)$$

where L_E is a modification due to the eddy currents. Essentially this change amounts merely to representing the effect of eddy currents by means of a hypothetical damper winding on the direct axis, a representation already well known, but the discussion given in Section 6.3 is necessary to establish that the effect of such a winding in this particular test would be to reduce all the apparent inductance values by the *same* amount, L_E. Admittedly the representation proposed is a first approximation only; the equivalence established in Section 6.2 relates to total fluxes, and a finer simulation would be provided by postulating damper windings on both axes. It is felt, however, that the object of explaining these particular experimental results is sufficiently attained by the hypothesis of a damper winding on the direct axis only; it is not intended to imply that such a hypothesis would necessarily be adequate in all circumstances.

It is therefore possible, by comparing eqns. (19) with the curves of Fig. 2, to find values for the differences between the coefficients L_S, L_D and L'_D (though not their ratios). Working from the fundamental component of the self-inductance curve, it is found that $L_S - L_E$ is 13·8 mH, $L_D - L_E$ is −5·1 mH, and $L'_D - L_E$ is ±1·5 mH, the sign to be attributed to the mutual inductance being uncertain. From this it may be deduced that $L'_D - L_D$ is either 3·6 or 6·6 mH; in other words, in the normal operating condition (when there are no eddy currents in the pole iron) L_D and L'_D are unequal, L'_D being the greater. This is in accordance with calculation (see Table 1).

No further information relevant to the operating condition can be deduced from the standstill test.

(3.2) Tests Verifying Equivalent Circuit for Representation of Eddy Currents

The conclusions of the preceding Section, based as they are upon an assumed equivalent circuit for representing the effect of eddy currents, may be regarded as somewhat insecurely founded unless the equivalent circuit can be proved valid by other means. Such a proof should cover a range of speeds, since varying the speed varies the magnitude of the eddy currents.

The following measurements were made with the machine driven at speeds corresponding to slips $s = +1$ to $s = -1$.

(*a*) Alternating current of 1·5 A was supplied to one phase of the armature; the fundamental component of the induced voltage in the other phase was measured.

(*b*) Two-phase currents of 0·1 A were supplied to the armature; the fundamental component of the terminal voltage for one phase was measured.

The results of both tests are given in Fig. 3. The isolation of the fundamental component required the use of a wave filter of the electronic type, for which Q equalled 100 at 50 c/s. Readings were not taken at speeds corresponding to $s = 0$, 0·5 or 1,

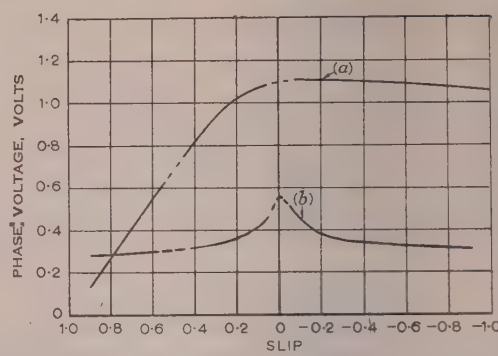

Fig. 3.—Fundamental component of phase voltage.

(*a*) Voltage induced in one phase when a current of 1·5 A is passed through the other.
(*b*) Terminal voltage of one phase when both phases are supplied with 2-phase currents of 0·1 A.

because at these speeds one or other of the component frequencies in the voltage wave, varying with s, passes through the fundamental frequency and gives an adventitious value to the total fundamental component at that point.

The theory underlying these tests involves some heavy algebra and is not reproduced here. It is sufficient to state that the leading features of both curves in Fig. 3 can be accounted for by a theory which uses the equivalent circuit proposed in Section 6.2.

(3.3) Measurements with Machine Driven at Various Speeds

The ideas underlying the tests about to be described were the following:

(*a*) To obtain an extra degree of freedom for the measurements by varying the speed. If a quantity can be measured for a number of speeds near synchronism but not at the synchronous speed itself, its value at synchronism can be found by interpolation or extrapolation.

(*b*) To take advantage of the symmetry and greater simplicity associated with the use of a polyphase supply.

Reference to the basic machine equations (3) will show that it is easy to calculate the phase voltages when the phase currents are specified, but difficult to calculate the currents from a knowledge of the voltages. The armature windings were therefore supplied with balanced 2-phase currents, and the test consisted in observing and analysing the phase-voltage waveform for various speeds. If, in the second of eqns. (3), the following current values are substituted:

$$i_f = 0 \quad i_a = I\cos\omega t \quad i_b = I\sin\omega t$$

and if r_a is neglected, it is found that

$$v_a = I\frac{d}{dt}[(L_S + L_D\cos 2\theta)\cos\omega t + L'_D\sin 2\theta\sin\omega t]$$

When θ is replaced by $(1-s)\omega t$ and X_S written for $L_S\omega$, we find

$$v_a = -X_S I\left[\sin\omega t + (1-2s)\left(\frac{\lambda+\lambda'}{2}\right)\sin(1-2s)\omega t + (3-2s)\left(\frac{\lambda-\lambda'}{2}\right)\sin(3-2s)\omega t\right] \quad (20)$$

where $\lambda = L_D/L_S \quad \lambda' = L'_D/L_S$ . . . . (21)

For small values of the slip s, the $2s\omega t$ part of the sines may be

egarded as a slow change of phase, and eqn. (20) resolves itself nto two components:

(i) A 'fundamental' term whose magnitude varies in the ratio $1 + (1 - 2s)(\lambda + \lambda')/2$ to $1 - (1 - 2s)(\lambda + \lambda')/2$.

(ii) A 'third-harmonic' term whose magnitude relatively to the mean of the 'fundamental' is $(3 - 2s)(\lambda - \lambda')/2$.

f these terms can be identified in the voltage oscillograms and heir ratios measured, it becomes possible to calculate the ratio λ/λ', which is equal to L_D/L'_D.

A series of oscillograms taken at values of slip ranging from early zero to nearly 2 is reproduced in Fig. 4. They display he following features.

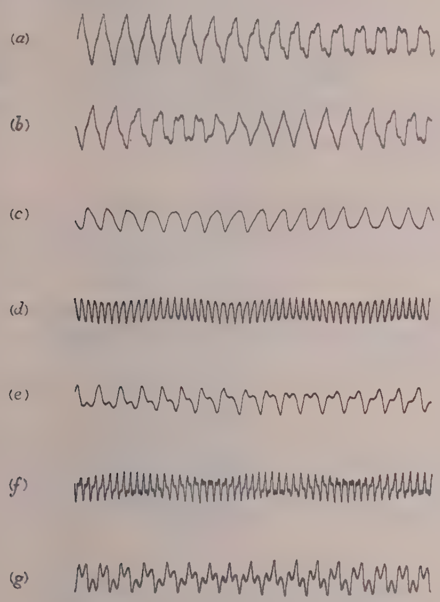

Fig. 4.—Oscillograms of phase voltage with a 2-phase current supply.

Figs. 4(*a*) *and* (*b*).—Taken at small values of slip, these waveorms show both the amplitude-modulated 'fundamental' and he gliding 'third-harmonic' components predicted by eqn. (20). Other harmonics are also present; to account for these it is ecessary to take into consideration higher-order terms in the ductance functions [eqns. (2)].

Figs. 4(*c*) *and* (*d*).—These oscillograms are taken, with two fferent time scales, at a slip close to 0·5—a speed possessing special interest since [according to eqn. (20)] v_a should consist only of a fundamental and a second-harmonic term, and the ratio of the latter to the former should be $|\lambda - \lambda'|$. Both these components can be seen to be present. The slow modulation effect is due to a small departure from $s = 0\cdot5$.

Figs. 4(*e*) *and* (*f*).—Here the slip is nearly 1·5, a value at which eqn. (20) predicts a second harmonic having a ratio of $(\lambda + \lambda')$ to the fundamental—a larger ratio than that found in Figs. 4(*c*) and (*d*). Inspection of the waveforms confirms this.

Fig. 4(*g*).—At a slip approximating to 2, the chief harmonic should be a third harmonic of relative value

$$3(\lambda + \lambda')/(2 + \lambda - \lambda')$$

It can be clearly seen.

The oscillograms provide verification that the expected phenomena are actually occurring, but the quantitative analysis of the waveforms was performed with a wave analyser, which measures the voltage in each harmonic component. The result of this analysis, for small values of slip, is set forth in Table 2. It is to be noted that this test cannot distinguish between λ and λ'; it is assumed on the basis of Sections 2 and 3.1 that $\lambda < \lambda'$. (The relations between the λ's of Table 2 and the k's of Table 1 are $\lambda = k_2p_2/k_0p_0$ and $\lambda' = k'_2p_2/k_0p_0$.)

The variation in the ratio λ/λ', or L_D/L'_D, is seen to be small over the range of slips considered, and the limiting value as $s \to 0$ is close to 0·31 for any of the current values. The theoretical value derived from Table 1 ($q = 4$) is 0·603.

(4) CONCLUSIONS

Starting from the basic equations (3) and taking account of eddy currents in the pole iron, it has proved possible to account for the main features of the curves and oscillograms obtained. The general validity of the methods used appears to be established, and it is possible to feel some confidence in the conclusion of Section 3.3 that the ratio L_D/L'_D is about 0·31 in the machine tested. The chief point left open is the discrepancy between this figure and the theoretical value of 0·603 derived in Section 2. This is probably due to the use of an over-simple electromagnetic model which fails to take account of leakage flux. It is to be expected that flux leakage will reduce the variation in inductance, represented by the second term of the expression $L_S + L_D \cos 2\theta$; on the other hand, it will not affect the mutual inductance between phases, designated by $L'_D \sin 2\theta$. Thus its effect will be to reduce the ratio L_D/L'_D below the ideal value of 0·603. However, even 0·603 is very different from the value of unity tacitly postulated in the two-axis theory of machines, and it seems impossible to escape from the conclusion that, in its application to salient-pole machines, the two-axis theory can be only an approximation to the truth.

Table 2

MEASURED INDUCTANCE RATIOS IN A 2-PHASE ALTERNATOR

Slip s	Harmonic frequencies		Voltage ratio B/A at phase currents (p.u.):			Ratio of inductance coefficients λ/λ' at phase currents (p.u.):		
	A $(1 - 2s)f$	B $(3 - 2s)f$	0·022	0·033	0·044	0·022	0·033	0·044
	c/s	c/s						
0·15	35	135	$2\cdot0_2$	$2\cdot1_0$	$2\cdot0_2$	$0\cdot31_2$	$0\cdot29_5$	$0\cdot31_2$
0·20	30	130	$2\cdot3_1$	$2\cdot3_9$	$2\cdot3_3$	$0\cdot30_4$	$0\cdot28_9$	$0\cdot30_0$
0·25	25	125	$2\cdot6_8$	$2\cdot7_6$	$2\cdot7_1$	$0\cdot30_2$	$0\cdot28_8$	$0\cdot29_7$
0·30	20	120	$3\cdot3_0$	$3\cdot3_9$	$3\cdot3_9$	$0\cdot29_0$	$0\cdot27_8$	$0\cdot27_8$
0·35	15	115	$4\cdot2_7$	$4\cdot5_2$	$4\cdot6_0$	$0\cdot28_5$	$0\cdot25_8$	$0\cdot25_0$

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(6) APPENDICES

(6.1) Inductance Coefficients for Uniformly Distributed 2-Phase Windings

The conductors of phase a are distributed in q slots over the arc of the armature periphery between the points $\alpha = \pi/2$ and $\alpha = 3\pi/2$. Their centres are therefore located at the points

$$\alpha = (q+1)\frac{\pi}{4q},\ (q+3)\frac{\pi}{4q},\ \ldots\ (3q-1)\frac{\pi}{4q} \quad . \quad (22)$$

Assuming the current to be concentrated at the slot centres, the function $h(\alpha)$ varies stepwise as follows:

$$\begin{aligned} h(\alpha) &= N \text{ when } 0 < \alpha < (q+1)\frac{\pi}{4q} \\ &= N\left(1 - \frac{2}{q}\right) \text{ when } (q+1)\frac{\pi}{4q} < \alpha < (q+3)\frac{\pi}{4q} \\ &= N\left(1 - \frac{4}{q}\right) \text{ when } (q+3)\frac{\pi}{4q} < \alpha < (q+5)\frac{\pi}{4q} \\ &\quad \ldots \\ &= \frac{N}{q} \text{ when } (2q-2)\frac{\pi}{4q} < \alpha < \frac{\pi}{2} \text{ if } q \text{ is odd} \\ \text{or} &\quad 0 \text{ when } (2q-1)\frac{\pi}{4q} < \alpha < \frac{\pi}{2} \text{ if } q \text{ is even} \quad . \quad (23) \end{aligned}$$

When this $h(\alpha)$ is substituted in eqn. (8), the expression for self-inductance takes the form

$$L_{aa} = k_0 N^2 p_0 + k_2 N^2 p_2 \cos 2\theta + \ldots \quad . \quad . \quad (24)$$

where k_0 is given by

$$\begin{aligned} k_0 &= \frac{1}{N^2}\int_0^{\pi/2} [h(\alpha)]^2 d\alpha \\ &= (q+1)\frac{\pi}{4q} + \frac{\pi}{2q}\left[\left(1-\frac{2}{q}\right)^2 + \left(1-\frac{4}{q}\right)^2 + \ldots\right] \end{aligned}$$

the last term of the series being $(1/q)^2$ or $(2/q)^2$ according as q is odd or even; so that

$$k_0 = (q+1)\frac{\pi}{4q} + \frac{\pi}{2q^3}[1^2 + 3^2 + 5^2 + \ldots + (q-2)^2](q \text{ odd})$$

or

$$k_0 = (q+1)\frac{\pi}{4q} + \frac{\pi}{2q^3}[2^2 + 4^2 + 6^2 + \ldots + (q-2)^2](q \text{ even})$$

The sum of either series is $\frac{1}{6}q(q-1)(q-2)$; from this, we find

$$k_0 = \frac{\pi}{6}\left(2 + \frac{1}{q^2}\right) \quad . \quad . \quad . \quad . \quad . \quad (15)$$

Furthermore, k_2 is given by

$$k_2 N^2 \cos 2\theta = \tfrac{1}{2}\int_0^{\pi/2} [h(\alpha)]^2 [\cos(2\theta + 2\alpha) + \cos(2\theta - 2\alpha)]d\alpha$$

or

$$\begin{aligned} k_2 &= \frac{1}{N^2}\int_0^{\pi/2} [h(\alpha)]^2 \cos 2\alpha d\alpha \\ &= \int_0^{(q+1)\pi/4q} \cos 2\alpha d\alpha + \left(1 - \frac{2}{q}\right)^2 \int_{(q+1)\pi/4q}^{(q+3)\pi/4q} \cos 2\alpha d\alpha + \ldots \end{aligned}$$

the last term being $\frac{1}{q^2}\int_{(2q-2)\pi/4q}^{\pi/2} \cos 2\alpha d\alpha$ when q is odd, and $\left(\frac{2}{q}\right)^2\int_{(2q-3)\pi/4q}^{(2q-1)\pi/4q} \cos 2\alpha d\alpha$ when q is even. When the integrations are performed, the result is

$$k_2 = \frac{1}{2}\cos\frac{\pi}{2q} - \frac{1}{q^2}\sin\frac{\pi}{2q}\left[\cos\frac{\pi}{2q} + 3^2\cos\frac{3\pi}{2q} + \ldots + (q-2)^2\cos(q-2)\frac{\pi}{2q}\right]$$

or

$$k_2 = \frac{1}{2}\cos\frac{\pi}{2q} - \frac{1}{q^2}\sin\frac{\pi}{2q}\left[2^2\cos\frac{2\pi}{2q} + 4^2\cos\frac{4\pi}{2q} + \ldots + (q-2)^2\cos(q-2)\frac{\pi}{2q}\right]$$

according as q is odd or even. The summation of the bracketed series may be carried out by differentiating the identities:

$$\cos\theta + \cos 3\theta + \ldots + \cos m\theta \equiv \frac{\sin(m+1)\theta}{2\sin\theta}$$

and $\cos 2\theta + \cos 4\theta + \ldots + \cos m\theta \equiv \dfrac{\sin(m+1)\theta - \sin\theta}{2\sin\theta}$

In this way it is found that, whether q is odd or even,

$$k_2 = \frac{\cos(\pi/2q)}{q^2\sin^2(\pi/2q)} \quad . \quad . \quad . \quad . \quad . \quad (16)$$

In the same way, $h(\alpha)$ and $h(\alpha + \frac{1}{2}\pi)$, defined by eqn. (23), can be substituted into eqn. (10). When the mutual inductance L_{ab} is written in the form

$$L_{ab} = k_2' N^2 p_2 \sin 2\theta + \ldots \quad . \quad . \quad . \quad (25)$$

it is found that

$$k_2' N^2 \sin 2\theta = \frac{1}{2}\int_0^{\pi/2} h(\alpha)h(\alpha + \tfrac{1}{2}\pi)[\cos(2\theta + 2\alpha) - \cos(2\theta - 2\alpha)]d\alpha$$

or

$$\begin{aligned} k_2' &= \frac{1}{N^2}\int_0^{\pi/2}[-h(\alpha)h(\alpha + \tfrac{1}{2}\pi)]\sin 2\alpha d\alpha \\ &= \int_{(q-1)\pi/4q}^{(q+1)\pi/4q} \sin 2\alpha d\alpha + \left(1 - \frac{2}{q}\right)\left[\int_{(q-3)\pi/4q}^{(q-1)\pi/4q} \sin 2\alpha d\alpha + \int_{(q+1)\pi/4q}^{(q+3)\pi/4q} \sin 2\alpha d\alpha\right] + \ldots \end{aligned}$$

the last term being

$$\frac{1}{q}\left[\int_0^{2\pi/4q} \sin 2\alpha d\alpha + \int_{(2q-2)\pi/4q}^{\pi/2} \sin 2\alpha d\alpha\right] \text{ when } q \text{ is odd}$$

$$\frac{2}{q}\left[\int_{\pi/4q}^{3\pi/4q}\sin 2\alpha d\alpha + \int_{(2q-3)\pi/4q}^{(2q-1)\pi/4q}\sin 2\alpha d\alpha\right] \text{ when } q \text{ is even}$$

After integration we obtain

$$k_2' = \frac{2}{q}\sin\frac{\pi}{2q}\left\{\frac{q}{2} + \left[\sin\frac{\pi}{2q} + 3\sin\frac{3\pi}{2q} + \dots + (q-2)\sin(q-2)\frac{\pi}{2q}\right]\right\}$$

$$k_2' = \frac{2}{q}\sin\frac{\pi}{2q}\left\{\frac{q}{2} + \left[2\sin\frac{2\pi}{2q} + 4\sin\frac{4\pi}{2q} + \dots + (q-2)\sin(q-2)\frac{\pi}{2q}\right]\right\}$$

ccording as q is odd or even. The summation is performed by e same device as before, leading to the result

$$k_2' = \frac{1}{q\sin(\pi/2q)} \quad . \quad . \quad . \quad . \quad . \quad (18)$$

(6.2) Equivalent Circuit for Representation of Eddy Currents

Consider a rectangular pole built up from n plates each of ickness $2b$ and width $w(\gg 2b)$. The eddy currents induced in ese plates by the passage of alternating flux will modify the agnitude and phase of the m.m.f. required to set up that flux, nd will introduce an additional loss, as compared with the state affairs in a perfectly laminated pole. It is required to investi-ate the simulation of the eddy-current effect by a short-circuited inding supposed wound on the latter.

Solutions to the problem of flux penetration into a lamination e to be found in textbooks on electromagnetism. It is neces-ry to assume a constant permeability to make a formal solution ossible, and it can be shown[7] that the flux Φ per unit width of e lamination is given in magnitude and phase by

$$\frac{B_0 b\sqrt{2}}{\beta}\left(\frac{1-j}{\sqrt{2}}\right)\left(\frac{\sinh\beta\cos\beta + j\cosh\beta\sin\beta}{\cosh\beta\cos\beta + j\sinh\beta\sin\beta}\right) \quad . \quad (26)$$

here

$$\beta = b\left(\frac{\eta_0\mu\omega}{2\rho}\right)^{1/2} \quad . \quad . \quad . \quad . \quad . \quad . \quad (27)$$

is the r.m.s. value of the flux density in the surface of the mination, where it is unaffected by the eddy currents, and is us related to the extraneous a.c. magnetizing force H_0 by the uation $B_0 = \eta_0\mu H_0$. By separating the real and imaginary rts in the flux equation, it may be proved that the application this magnetizing force to the complete pole sets up the follow-g components of flux: in phase with H_0,

$$\Phi_p = \frac{\Phi_0}{2\beta}\left(\frac{\sinh 2\beta + \sin 2\beta}{\cosh 2\beta + \cos 2\beta}\right) \quad . \quad . \quad . \quad (28)$$

nd in quadrature (lagging) with H_0,

$$\Phi_q = \frac{\Phi_0}{2\beta}\left(\frac{\sinh 2\beta - \sin 2\beta}{\cosh 2\beta + \cos 2\beta}\right) \quad . \quad . \quad . \quad (29)$$

here

$$\Phi_0 = (2nbw)(\eta_0\mu H_0) \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (30)$$

e quadrature component induces in the a.c. magnetizing inding an e.m.f. $\omega\Phi_q$ in opposition to the current, enabling e power dissipated in eddy-current loss to be drawn from the supply. From this it follows that the eddy-current loss per unit length of the pole is

$$P_e = \omega\Phi_q H_0 \quad . \quad . \quad . \quad . \quad . \quad . \quad (31)$$

In a perfectly laminated pole, the establishment of a flux Φ would require a magnetizing force of $\Phi/2\eta_0\mu nbw$. If a short-circuited winding were wound on such a pole it would contribute to the magnetizing force, and the extraneous magnetizing force would have to be modified to take account of this. Let the short-circuited winding have N_x turns and impedance $Z_X = r_x + jX_x$, and let the length of the pole be l; the e.m.f. induced in it by the flux Φ is $-j\omega N_x\Phi$ and the current $-j\omega N_x\Phi/Z_x$, so that the contribution from this winding to the m.m.f. is $-j\omega N_x^2\Phi/Z_x$. Thus the extraneous m.m.f. must be

$$F = \frac{\Phi l}{2\eta_0\mu nbw} + \frac{j\omega N_x^2\Phi}{r_x + jX_x}$$

$$= \Phi\left[\left(\frac{l}{2\eta_0\mu nbw} + \frac{\omega N_x^2 X_x}{Z_x^2}\right) + j\left(\frac{\omega N_x^2 r_x}{Z_x^2}\right)\right] \quad . \quad (32)$$

For comparable conditions between the real pole and the idealized model, F must equal $H_0 l$. Inserting this value and multiplying up by $2\eta_0\mu nbw$, we obtain

$$\Phi_0 = \Phi[(1 + y_x X_x) + jy_x r_x] \quad . \quad . \quad . \quad . \quad (33)$$

where

$$y_x = \frac{(\eta_0\mu\omega)(2nbw)N_x^2}{lZ_x^2} \quad . \quad . \quad . \quad . \quad . \quad (34)$$

Thus

$$\Phi_p = \Phi_0\frac{1 + y_x X_x}{(1 + y_x X_x)^2 + (y_x r_x)^2} \quad . \quad . \quad . \quad (35)$$

and

$$\Phi_q = \Phi_0\frac{y_x r_x}{(1 + y_x X_x)^2 + (y_x r_x)^2} \quad . \quad . \quad . \quad (36)$$

The two quantities $y_x r_x$, $y_x X_x$ are at our disposal, and it only remains to determine whether, for any assigned value of β, it is possible to find values for $y_x r_x$ and $y_x X_x$ such that eqn. (35) shall give the same value of Φ_p as eqn. (28), and eqn. (36) the same value of Φ_q as eqn. (29). It may be verified that the appropriate values are given by

$$y_x r_x = \beta\left(\frac{\sinh 2\beta - \sin 2\beta}{\cosh 2\beta - \cos 2\beta}\right) \quad . \quad . \quad . \quad (37)$$

$$1 + y_x X_x = \beta\left(\frac{\sinh 2\beta + \sin 2\beta}{\cosh 2\beta - \cos 2\beta}\right) \quad . \quad . \quad . \quad (38)$$

Since the quantities on the right-hand side of these equations can be shown to be essentially greater than 0 and 1 respectively, it is always possible to simulate the properties of an imperfectly laminated pole by means of a perfectly laminated pole carrying a short-circuited winding.

(6.3) Effect of Eddy Currents in Pole System upon Apparent Values of Resistance and Inductance in a Stationary Machine

The effect of the eddy currents is simulated by supposing a winding of inductance L_x and resistance r_x to be located on the poles and short-circuited. When the field winding is open and alternating current is supplied to one or both of the armature phases, the performance of the machine is described by equations closely allied to eqns. (3), namely

$$\begin{bmatrix} 0 \\ v_a \\ v_b \end{bmatrix} = \begin{bmatrix} L_x D + r_x & L_{xd}D\cos\theta & L_{xd}D\sin\theta \\ L_{xd}D\cos\theta & D(L_S + L_D\cos 2\theta) + r_a & L_D' D\sin 2\theta \\ L_{xd}D\sin\theta & L_D' D\sin 2\theta & D(L_S - L_D\cos 2\theta) + r_a \end{bmatrix} \begin{bmatrix} i_x \\ i_a \\ i_b \end{bmatrix} \quad . \quad . \quad . \quad . \quad . \quad (39)$$

In the test described in Section 3.1, there was a single-phase supply and the machine was stationary. Under these circumstances θ is constant, while i_x and i_a, being power-frequency currents, may be represented by phasors I_x and I_a, and $DI = j\omega I$. Thus the first two of eqns. (39) may be written

$$\left.\begin{aligned} 0 &= (r_x + j\omega L_x)I_x + j\omega L_{xd}\cos\theta I_a \\ V_a &= j\omega L_{xd}\cos\theta I_x + [r_a + j\omega(L_S + L_D\cos 2\theta)]I_a \end{aligned}\right\} \quad (40)$$

Eliminating I_x, we obtain (writing X for $j\omega L$ wherever this appears)

$$V_a = (r_e + jX_e)I_a \quad . \quad . \quad . \quad . \quad . \quad . \quad (41)$$

where

$$r_e = r_a + r_x\left(\frac{X_{xd}}{Z_x}\right)^2\cos^2\theta \quad . \quad . \quad . \quad . \quad (42)$$

and $X_e = X_S + X_D\cos 2\theta - X_x\left(\frac{X_{xd}}{Z_x}\right)^2\cos^2\theta$

which may be written

$$X_e = \left[X_S - \frac{X_x}{2}\left(\frac{X_{xd}}{Z_x}\right)^2\right] + \left[X_D - \frac{X_x}{2}\left(\frac{X_{xd}}{Z_x}\right)^2\right]\cos 2\theta \quad (43)$$

The terms added to r_a in eqn. (42) and subtracted from X_S and X_D in eqn. (43) show the effect of the coupled eddy-current circuit in modifying the apparent constants of the armature winding. In particular, if

$$X_D < \frac{X_x}{2}\left(\frac{X_{xd}}{Z_x}\right)^2 < X_S$$

the apparent inductance will be greater on the quadrature axis than on the direct axis—as is the case in the readings depicted in Fig. 2.

The induced voltage in the unexcited phase is given by

$$V_b = j\omega L_{xd}\sin\theta I_x + j\omega L'_D\sin 2\theta I_a \quad . \quad . \quad (44)$$

and when substitution is made for I_x from eqns. (40), it is found that

$$V_b = \left\{\frac{r_x}{2}\left(\frac{X_{xd}}{Z_x}\right)^2 + j\left[X'_D - \frac{X_x}{2}\left(\frac{X_{xd}}{Z_x}\right)^2\right]\right\}\sin 2\theta I_a \quad (45)$$

It is probable that the real term will have little effect upon the magnitude of V_b; but the negative term in the square bracket may materially reduce V_b (as in Fig. 2) or may even reverse its sign.

It will be noted that the effect of the eddy currents is to reduce all the reactances by the same amount, so that the coefficients L_S, L_D and L'_D become $(L_S - L_E)$, $(L_D - L_E)$ and $(L'_D - L_E)$, where

$$L_E = \frac{1}{2}L_x\left(\frac{X_{xd}}{Z_x}\right)^2 \quad . \quad . \quad . \quad . \quad . \quad (46)$$

This is used in eqn. (19).

DISCUSSION ON 'THE TWO-PHASE INDUCTION MOTOR USED AS A SERVO MOTOR'*

Dr. E. R. Laithwaite (*communicated*): The author does not state in the list of symbols whether q is the ratio of resistance to magnetizing reactance or of resistance to leakage reactance. It is clear, however, from the method of experimental measurement of q in Section 8 that if the magnetizing reactance is much greater than the leakage reactances, as it would be in a good machine, the result obtained for q is almost exactly equal to the ratio of resistance to leakage reactance.

The performance of the motor as a single-phase machine can never be predicted from the knowledge of the ratio of resistance to leakage reactance alone. Nevertheless it is this value of q which is used throughout the body of the paper to predict the performance when the motor is neither a true polyphase machine nor a true single-phase machine.

Eqn. (5) is a clear statement that the torque of the single-phase motor is calculable by subtracting the torques due to two counter-rotating fluxes of equal magnitude. Section 8 shows that, if the constant-voltage speed/torque curve of the machine run as a polyphase motor does not have a maximum to the right of the speed origin, q will exceed unity, and eqn. (5) predicts that the motor will never run as a single-phase machine. The following test results relate to a 56 hp slip-ring induction motor which was subjected to the test described in Section 8. Fig. A, curve (i), shows the speed/torque curve when sufficient rotor resistance had been inserted to ensure that maximum torque

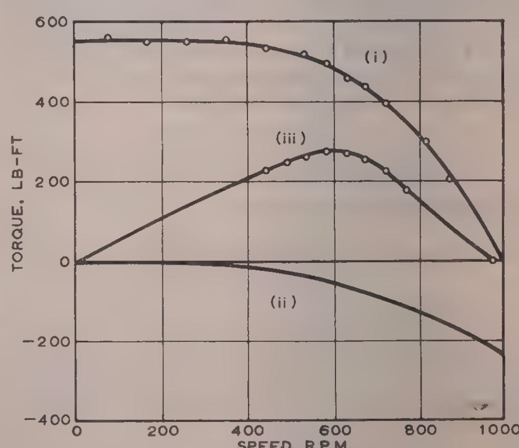

Fig. A.—Speed/torque curves.
(i) Experimental curve, polyphase operation.
(ii) Predicted performance, single-phase operation.
(iii) Experimental curve, single-phase operation.

* CONNELLY, D.: Paper No. 3280 M, August, 1960 (see 107 A, p. 366).

ccurred in the vicinity of zero speed. Curve (ii) shows the erformance as a single-phase machine as predicted by the uthor's method. Curve (iii) is an experimental curve for the achine run single phase. Fig. B, curve (i), shows the running-

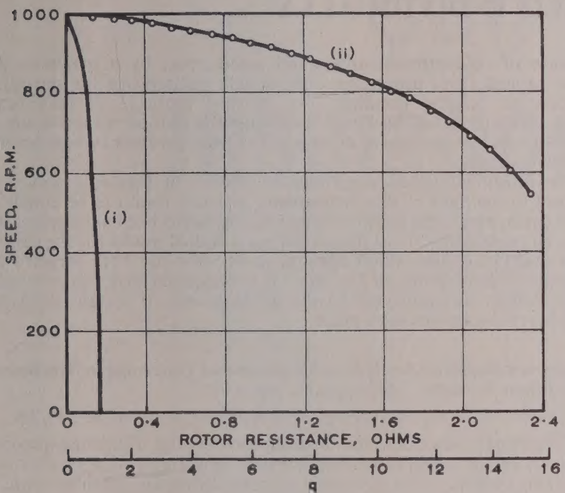

Fig. B.—Running-light speed as a function of rotor resistance.
(i) Predicted performance. (ii) Measured performance.

ght speed as a function of q, calculated by the author's method, orresponding to his curve for $\alpha = 0$ (Fig. 6). The total fective rotor resistance per phase for Fig. A, curve (i), was ·156 ohm, indicating that the reactance per phase was approxi- ately equal to this value. It was found possible to insert a rther resistance of 2·2 ohms per phase (over 14 times the value f X) before the motor failed to run. Fig. B, curve (ii), shows the nning-light speeds measured experimentally at various values f rotor resistance. The corresponding values of q calculated ccording to the author's Section 8 are also marked on the scale.

In small machines, where the primary leakage impedance is uch greater in relation to the magnetizing reactance than it is good machines, comparisons between the performance pre- icted as outlined in Section 8 and experimental results are uch less likely to show serious errors, and this is illustrated y the author's Fig. 8, in which the two sets of curves are in ubstantial agreement. Fig. A illustrates how very different the esults are when a larger machine with a low primary impedance nd a high magnetizing reactance is used, and designers of large ervo-motors must take this into account.

Dr. V. Ahmad (*India: communicated*): The author's design of 2-phase servo induction motor, using two stators, and the erivations of the general expressions (2) and (3) from the undamental, are very ingenious and quite interesting. The onstruction of the machine is rather complicated, and apart from obtaining a 'stable-zero' characteristic for servo applica- tions, I do not see any marked improvement in the torque/speed characteristics over those obtained by the much more convenient voltage phase and amplitude control schemes.

I feel that the author should have interpreted the expressions in terms more familiar to the design engineers: the rotor phase current is the sum of the positive- and negative-sequence current components, and the output torque is the algebraic sum of the positive-sequence (motoring) and negative-sequence (braking) torque components, under any condition of operation. The various results discussed in the paper could then have been explained with greater clarity.

In my opinion, the new definition of synchronous speed given by the author that, in general, it is the speed at which the unidirectional torque is zero, is quite unnecessary and very mis- leading. Space does not permit me to go into any detail, but following the idea of sequence currents and torques, it can easily be explained why the unidirectional torque of a single- phase induction motor can occur only at a sub-synchronous speed, called the no-load speed, and how the characteristics shown in Fig. 3 may be obtained simply by adding resistances to the secondary circuit of any single-phase induction motor.

Overheating of the machine, discussed in Section 5.2, is perhaps due to the increase in the sequence currents under the condition of unbalanced operation, but is more probably due to the increase in the saturation of the stator and rotor iron; otherwise, since the total power in the machine in question is delivered through two stators, the latter should not have been overloaded. The increased skin effect may be responsible for the noted increase in the rotor resistance/reactance ratio q.

Mr. D. Connelly (*in reply*): I agree that there is lack of preciseness in the definition of q, as Dr. Laithwaite states. In the original analysis q was considered to be the total reactance, i.e. the reactance associated with both the leakage flux and that interlinking both stator and rotor (giving the magnetizing reactance), but the initial assumption that the flux density was the resultant of that caused by both stator and rotor currents, instead of being that caused by the stator current alone, ulti- mately precluded the magnetizing reactance. This did not become evident from the experimental work carried out on the small machines available to me for test. The position is actually not quite as Dr. Laithwaite shows in his Fig. 1 for his experimental points could indicate a maximum at a slip of 0·675 instead of unity, which corresponds with a positive torque and therefore a free-running single-phase machine.

In relation to Dr. Ahmad's comment, the condition for stable zero speed is applicable to all three methods of use of the 2-phase machine as a servo motor, not only to that of Fig. 1.

May I explain that the paper originated in an attempt to deduce only performance characteristics, in particular of the machine shown in Fig. 1. The design problem was not dealt with, but I must agree in view of the poor performance of this machine that it might have been advantageous to consider it more fully.

MONOGRAPHS PUBLISHED INDIVIDUALLY

Summaries are given below of monographs which have been published individually, price 2s. each (post free). Applications, quoting the serial number as well as the authors' names, and accompanied by a remittance, should be addressed to the Secretary.

Certain Approaches to Electromagnetic Field Problems pertaining to Dynamo-Electric Machines. Monograph No. 438 U.

K. C. MUKHERJI, B.E., Ph.D.

Current-carrying circuits in electrical rotating machines give rise to electromagnetic fields which are modified by surface polarities induced in adjacent ferromagnetic media and by eddy currents induced in neighbouring conducting media. The paper reviews certain approaches towards solving some of the electromagnetic problems involved and introduces a method for taking account of the reaction of eddy currents induced in a ferromagnetic medium on their inducing field.

Internal Waveform Distortion in Silicon-Iron Laminations for Magnetization at 50 c/s. Monograph No. 446 M.

Prof. F. BRAILSFORD, Ph.D., and J. M. BURGESS, B.Sc.(Eng.).

The internal distribution of magnetic flux and eddy currents to be expected in a homogeneous magnetic lamination having a sinusoidal total flux has been investigated experimentally using an analogue. It is concluded that the anomalous loss found to occur in practice in laminations cannot be accounted for by waveform distortions within the lamination.

The Motion of Cold-Cathode Arcs in Magnetic Fields. Monograph No. 447 S.

A. E. GUILE, Ph.D., B.Sc.(Eng.), T. J. LEWIS, Ph.D., M.Sc., B.Sc.(Eng.), and P. E. SECKER, Ph.D., B.Sc.(Eng.).

The paper first discusses the way in which theories concerning the motion of cold-cathode arcs when acted upon by a magnetic field have ranged from proposing cathode-fall mechanisms for retrograde motion to column processes for forward motion. A very recent mechanism proposed by Ecker for retrograde motion is then examined and found to be capable of extension to higher pressures where forward motion occurs.

Previously published experimental results for forward motion are viewed in the light of this mechanism, which is found to be consistent with them, and some features which had hitherto been anomalous may now be explained. Thus there emerges a unified model for the cathode spot and fall region which appears to be valid for both forward and retrograde movements of the arc. It is suggested that this provides a vital step in the approach to the development of certain devices in which arc discharges take place.

Frequency-Response Analysis of Displacement Governing in Synchronous Power Systems. Monograph No. 448 S.

P. A. W. WALKER, B.Eng., and A. S. ALDRED, M.Sc., Ph.D.

The paper describes the application of the frequency-response concept to the analysis of displacement governing in a synchronous machine system. The governor loop is shown to combine with the basic closed-loop pattern of the generator and modifies only the feedback element in the original basic loop. The analysis is used to compare stability boundaries for different governor conditions, these boundaries being derived by the application of the Nyquist stability criterion. The results obtained by this method, although based on small-displacement theory, were found to be in agreement with those obtained from a power-system simulator.

The effects of phase lags, arising in the steam header of the turbine and the servo mechanism operating the throttle valve, on the stability of the system are considered together with that of a second-derivative stabilizer. An examination is also made of the damping in the system as this provides a useful appraisement when comparing the relative stability of different configurations in the governor loop.

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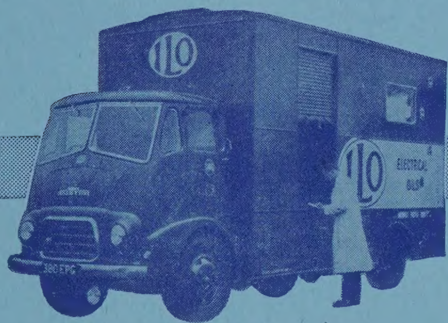

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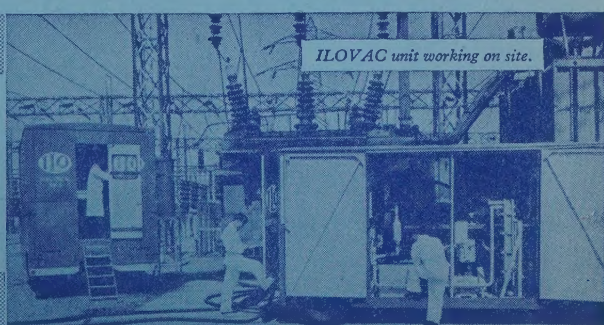

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